SEPTEMBER 1958

# WELDING JOURNAL

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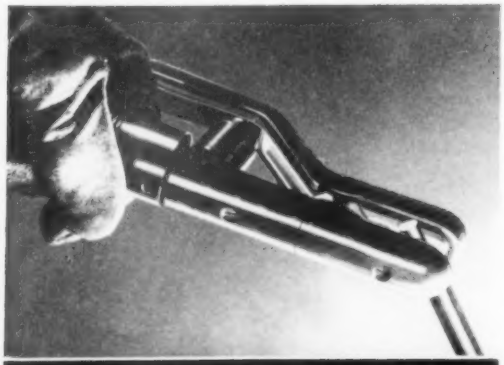
# JACKSON for ARC WELDING



Insulated Copper Alloy Holders have greatest strength, hardness and conductivity. Jackson insulators have highest heat and impact resistance. Choose them where greater duty cycles are called for.

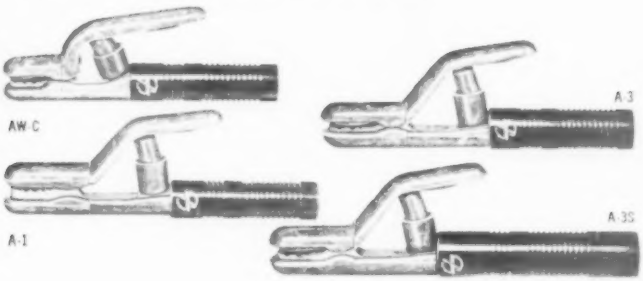
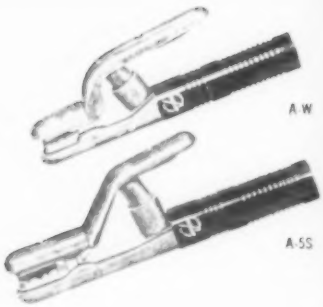


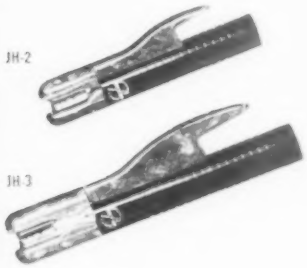

Table shows duty cycles possible with Jackson Insulated Copper Alloy Holders, using electrodes and cables shown.

| Holder Class | Model | Electrode | Amps & Cable | Amps & Cable | Duty Cycle |
|---|---|---|---|---|---|
| Small | AW-C | 7/32" | 250—No. 1 | 300—1/0 | 50% |
| Medium | A-1 | 1/4" | 300—1/0 | 350—2/0 | 50% |
| Large | A-3 | 3/8" | 375—2/0 | 450—3/0 | 65% |
| Heavy-Duty | A-3S | 3/8" | 500—4/0 | | 80% |




Featherlight medium class model A-W takes electrodes through 1/4" at 300 amps, weighs 14 oz. Featherlight larger model A-5S has wider tong and wider handle, takes electrodes through 5/16" at 400 amps.

Featherlight Holders have tongs of aluminum alloy, replaceable jaws of high-copper alloy. Excellent all-purpose holders, each within its rated capacity. Cable connection must be made mechanically.




Featherlight and streamlined, small class JH-2 takes electrodes through 5/32" at 200 amps, weighs 9 1/2 oz. Medium class JH-3 takes electrodes through 1/4" at 300 amps.

'Quik-Trik' Cable Connectors 2/0-R and 4/0-R have neoprene rubber insulators with tapered ends to stretch over cable snugly. They lock out moisture and dirt, don't catch against obstructions.

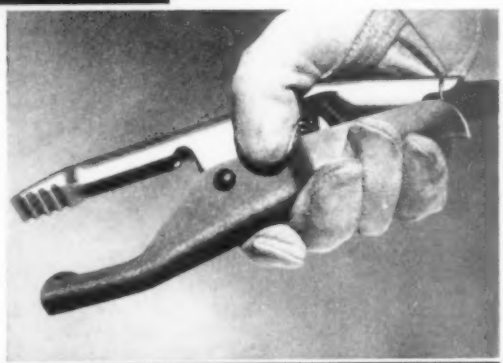
Cable Splicers for permanent connections. Copper alloy with hard fiber insulators.

Cable Lugs of cast copper alloy, 180 and 45 degrees.

Jackson Ground Clamps, Cable Connectors, Splicers and Lugs are available in three sizes:

| MODEL | CABLE SIZE | CABLE CONNECTION | CAPACITY |
|---|---|---|---|
| No. 1 | 4 thru 1/0 | Mechanical, soldered, or brazed | 300 amps |
| 2/0—R* and 2/0 | 1/0 and 2/0 | Mechanical, soldered, or brazed | 500 amps |
| 4/0—R* and 4/0 | 3/0 and 4/0 | Soldered or brazed only | 500 amps |

*Cable Connectors only



Jackson Ground Clamps give broad metal-to-metal contact. They also fit the hand, go easy on, easy off. Angled socket reduces strain on cable, allows wire fastening for extra support of the cable.

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- 5 KW FULL TIME AC POWER for emergency power when not welding
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*The "PowrWeld" lets the operator change over from DC welding to AC power without leaving the work—without shouting instructions to a helper. He can take all his welding equipment and tools to the job and work uninterrupted until it is completed. Power to weld and power to use tools, alternately or simultaneously. The only combination of its type on the market today.*

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**AC for auxiliary power.** From this same generator, you can operate tools that are most essential to the complete welding operation—such as lights, grinders, drills, chipping hammers, without leaving the job for machine adjustments.

**AC for full time power.** From the same generator, 5 KW AC power may be used to power many different types of electrical equipment, either permanently or on a "standby" basis in case of failure of regular power sources. Equipment such as grinders, drills, saws, lights, compressors, sprayers, vibrators, pumps, hoists, etc. Or—such farm equipment as incubators, brooders, milking machines, water pumps, lights, hay driers, refrigerators and many other items of electrical equipment in use on the farm today. Write today for complete details on this new combination unit. Hobart Brothers Company, Box WJ-98, Troy, Ohio, Phone FE 2-1223.

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☐ Electrodes ☐ Accessories ☐ Other models of arc welders

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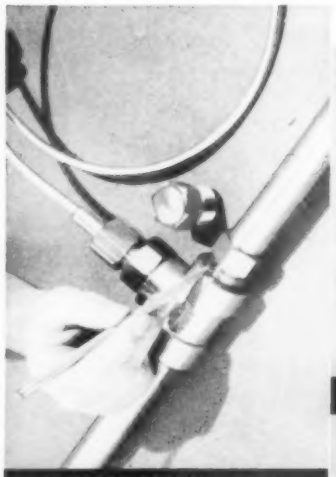
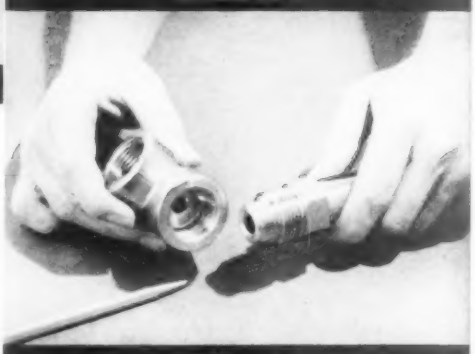
compound Pressure

# the new and better cylinder manifold...



only the bushing turns

threads do it



a very tight seat results

a simple wrench does the job

The new compound pressure* cylinder manifold is made of the sturdiest die forgings and extruded rods; all are drilled for uniform inner dimension. The leak proof joint which results from the compound pressure design is tighter and more leakproof even than the well understood and long used connection between regulator and cylinder valve. The big improvement of this new invention lies in these facts: you can have any competent mechanic assemble a cylinder manifold to your own or our specifications with the assurance of absolute alignment and permanent leak proofness. Yet, the resulting cylinder manifold may be disassembled at any time, stored or moved and reassembled when required. It may be extended when needed, redesigned when desired, stored in bins as individual fittings. Only the bushings move in assembly, thus absolute alignment is certain.

*patents applied for

by National SINCE 1910 of ...california

NATIONAL welding equipment company... 218 fremont street san francisco 5 california

535

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# Technical Welding Data: USS "T-1" Constructional Alloy Steel

## 4. Weld Metal Properties

As an aid to the engineer and designer, here are the properties of weld metal deposited by a few types of available electrodes. Other very good types of electrodes are now on the market.

**TYPICAL CHEMICAL COMPOSITION AND MECHANICAL PROPERTIES OF WELD METALS DEPOSITED BY HIGH STRENGTH ELECTRODES**

| AWS Class | Comp. Type | Weld-metal composition, % (undiluted deposit) | | | | | | | | | | |
|---|---|---|---|---|---|---|---|---|---|---|---|---|
| | | C | Mn | P | S | Si | Ni | Cr | Mo | V | Cu | Ti |
| E9015 | Mn-Mo | 0.07 | 1.45 | 0.025 | 0.024 | 0.39 | 0.06 | 0.08 | 0.35 | 0.02 | 0.14 | 0.013 |
| E11016 | Mn-Ni-Mo | 0.08 | 1.02 | 0.015 | 0.030 | 0.28 | 3.73 | 0.01 | 0.63 | 0.005 | 0.11 | 0.014 |
| E12015 | Ni-Mo-V | 0.06 | 1.12 | 0.011 | 0.018 | 0.40 | 2.11 | 0.11 | 0.95 | 0.21 | 0.11 | 0.005 |

| AWS Class | Comp. Type | Condition | All-weld-metal tensile properties (0.505" diam. specimen) | | | | |
|---|---|---|---|---|---|---|---|
| | | | Interpass Temp., F. | Yield Strength (0.2% Offset), psi | Tensile Strength, psi | Elong. in 2 in., % | Red. of area, % |
| E9015 | Mn-Mo | As-welded | 100 | 88,800 | 95,700 | 16.3 | 28.1 |
| E9015 | Mn-Mo | As-welded | Rising | 61,750 | 84,600 | 29.0 | 62.5 |
| E9015 | Mn-Mo | Stress-relieved (1100 F.) | 100 | 87,250 | 94,200 | 23.5 | 55.7 |
| E9015 | Mn-Mo | Stress-relieved (1100 F.) | Rising | 67,500 | 82,500 | 26.8 | 63.1 |
| E11016 | Mn-Ni-Mo | As-welded | 100 | 119,300 | 124,400 | 18.5 | 54.5 |
| E11016 | Mn-Ni-Mo | Stress-relieved (1100 F.) | 100 | 106,200 | 113,400 | 21.2 | 58.6 |
| E12015 | Ni-Mo-V | As-welded | 100 | 113,300 | 129,200 | 20.5 | 58.7 |
| E12015 | Ni-Mo-V | Stress-relieved (1100 F.) | 100 | 116,000 | 128,700 | 18.8 | 59.4 |

| AWS Class | Charpy-keyhole impact properties | | |
|---|---|---|---|
| | Comp. Type | Condition | Ductility-transition Temp., F. |
| E9015 | Mn-Mo | As-welded | minus 155 |
| E9015 | Mn-Mo | Stress-relieved (1100 F.) | minus 83 |
| E11016 | Mn-Ni-Mo | As-welded | minus 166 |
| E11016 | Mn-Ni-Mo | Stress-relieved (1100 F.) | minus 90 |
| E12015 | Ni-Mo-V | As-welded | minus 78 |
| E12015 | Ni-Mo-V | Stress-relieved (1100 F.) | above 130 |
| E12015 | Ni-Mo-V | Quenched from 1675 F., tempered at 1175 F. | 45 above zero |

*NOTE: Weld metals deposited by such electrodes on USS "T-1" Steel would have their chemical compositions altered by advancing of elements from the steel, and their yield and ultimate strengths would be increased.*

**REMEMBER THIS:** No other alloy steel offers the combination of properties that "T-1" Steel possesses. Here, in just one steel, you get very high yield strength (100,000 psi minimum), extraordinary toughness, great resistance to impact abrasion, and good high temperature strength. These let you reduce the size of highly stressed parts . . . save weight, reduce the amount of welding required and cut your costs. USS "T-1" Steel is being used in bridges, excavating equipment, pressure vessels, towers . . . in equipment that must take heavy abuse in sub-freezing temperatures . . . in equipment that must withstand heavy stresses and impact abrasion at temperatures up to 900° F.

**WRITE FOR COMPLETE INFORMATION** about the application and fabrication of USS "T-1" Steel. United States Steel, Room 2801, 525 William Penn Place, Pittsburgh 30, Pennsylvania. *USS and "T-1" are registered trademarks*

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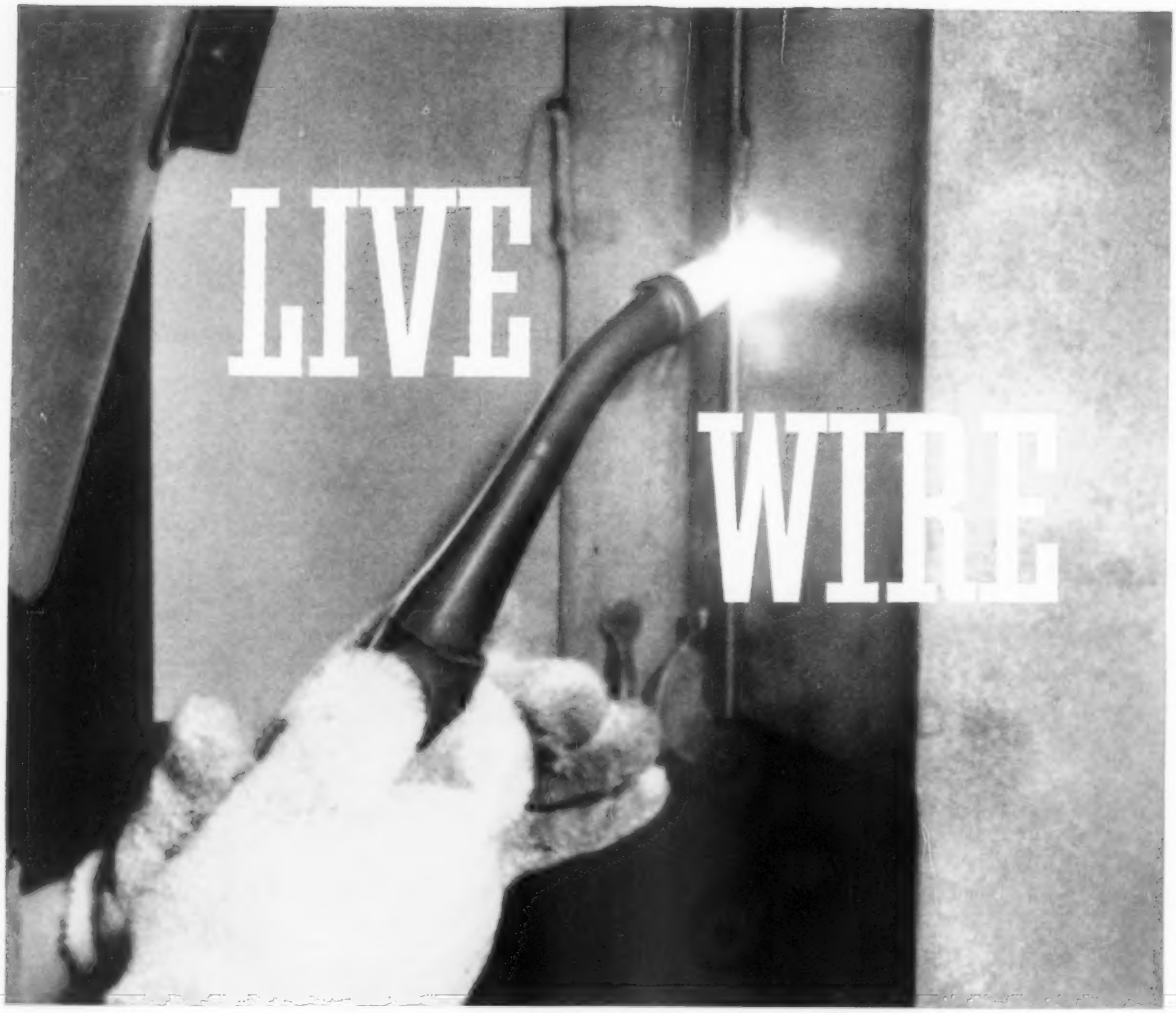
Columbia-Geneva Steel—San Francisco
Tennessee Coal & Iron—Fairfield, Alabama
United States Steel Supply—Steel Service Centers
United States Steel Export Company

USS United States Steel




## New design in Sigma hand welding torches

Here is a new, lightweight torch—only 16 ounces—for manually welding light-gage steels. Sigma ST-2 welds in all positions with no change in control or current settings. Welds .030- to .100-in. sheet, using low-voltage short-arc technique with .020- and .030-in. hard-drawn wire. For 200 amp continuous service, a-c or d-c.

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## for hard surfacing on low alloy steels and cast irons

### Extremely Hard

- *One-layer deposit gives you Brinell hardness of 500*
- *Two-layer deposit from 650 to 700 Brinell*

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- *Stable spray-type arc*
- *Excellent wash*
- *AC or DC*
- *Smooth bead contour*

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|---|---|---|
| 5/32" size | 110-150 | 110-140 |
| 3/16" size | 140-180 | 140-170 |
| 1/4" size | 170-240 | 170-230 |

**On DC use reverse polarity**

***Use Inco-Hard "1"* Electrode for:*** overlaying baffle plates, caterpillar heads, dipper bucket teeth, scraper lips . . . wherever parts need resistance to severe abrasion.

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## RESEARCH AND DEVELOPMENT

The terms research and development are used so frequently as a phrase combination that it is easy for a casual concept to develop that the two are synonymous. While in some types of industrial activity this may be true, in general a closer analysis of the true meaning of these terms will indicate significant differences. Once these differences have been recognized, a review of organizational structure and functions may reveal that research men are doing development work and vice versa, which does not contribute to optimum organizational efficiency.

Frequently, it is necessary to draw extreme analogies in order to point up differences. On this particular matter, we have found it helpful to define development work as aimed at producing some practical end result, usually of a tangible and physical nature, and research work as leading often to an end result that does not go beyond being set forth on a piece of paper. Working under this philosophy on a typical engineering matter, it is common to find that one or more answers will have been evolved by research men, following which or perhaps concurrently in the latter stages, development men have taken over to span the gap between theoretical concepts and practical aspects, including limitations, represented by the application.

Combining efforts in this manner has resulted in the elimination of many cases where an ideal theoretical solution to a problem actually would not work out in practice as well as another theoretical alternative that, although sound, was not quite so close to academic perfection. Beyond the issue itself, there are also human factors to consider. Some men are natural-born "gadget engineers." These men usually are clever, inventive and as a result like to use their ingenuity in solving problems that have a practical twist. Marked ability in this line of endeavor does not necessarily mean, however, that they will also do well in research work despite the fact that each requires an alert and inquisitive mind.

Again turning to what may appear as over-simplification, but nevertheless perhaps hinting at differences we are seeking, hand a problem to a research- and to a development-minded man. Each probably will reach for a pencil and paper, the research man to make calculations, the development man to draw sketches.

*C. E. Paules*
*VICE PRESIDENT*
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# 4 in 1

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## WELDERS/ POWER PLANTS

Hercules (Lycoming design) air cooled engine

**FIREBALL AMPERAGE RANGES**

| Tungsten Inert Gas | | Metallic Arc | |
|---|---|---|---|
| DC | AC | DC | AC |
| 35-100 | 35- 75 | 35- 80 | 35-100 |
| 85-225 | 65-160 | 75-175 | 65-160 |
| 170-375 | 150-375 | 125-350 | 110-400 |

**DAH-350 FIREBALL** four-in-one model is the only complete unit made to incorporate an ac-dc welder for (1) metallic arc, or (2) tungsten inert gas welding, plus (3) ac power plant, and (4) 1 KW dc power while welding. Twelve separate amperage ranges as shown above. Additional standard equipment features include a polarity switch, either continuous or "start only" high frequency and an automatic inert gas control panel with solenoid valve and postflow timer. Rated output at 100% duty cycle: 250 amps dc tungsten arc; 300 amps ac tungsten arc. Generator: 10 KW of 115/230v, single phase, 60 cycle ac.

**DA-300 BIG RIG.** Combination ac-dc welder, plus an ac power plant, plus 1 KW of dc power while welding, give this model three-in-one versatility. Generator rated at 10 KW of 115/230v, single phase, 60 cycle ac. Welding ranges in amperes are: (dc) 75-175 or 125-350; (ac) 65-160 or 110-400. Rated output at 100% duty cycle: 250 amps dc at 40 volts and 300 amps ac at 40 volts.

**D-250 ROUSTABOUT** provides a two-in-one arrangement whereby either of two dc welding current ranges — 75-175 amps or 125-350 amps — and 1 KW of 115v dc auxiliary power are available simultaneously. Rated output is 250 amps at 40 volts, 100% duty cycle. Generator produces 10 KW of 115/230v, single phase, 60 cycle ac.

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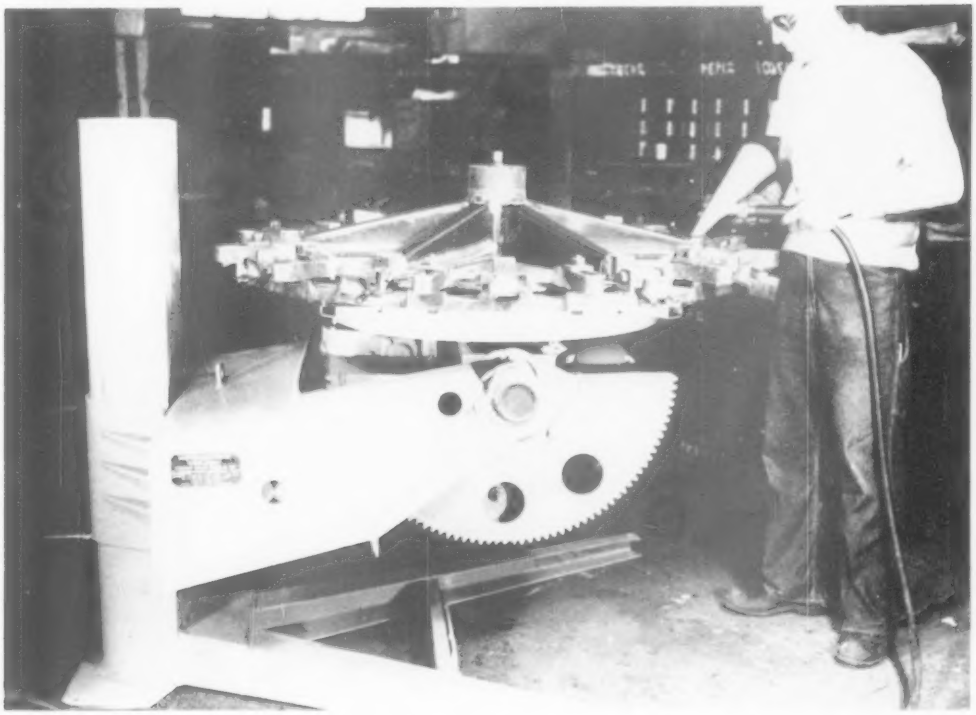


Rear drum-head design where parts are loaded into a fixture and completely welded with one setup operation

# DESIGN FOR PRODUCTION BY WELDING

*Weldments designed for optimum performance and fabrication economy, often combine castings, forgings, pressings and extrusions with plates and shapes*

BY JOHN MIKULAK

INTRODUCTION. Tradition and familiarity play an important role in influencing decisions in that they are the result of much effort that was expended in gaining the knowledge they represent. Generally, much time is lost and costs in research are increased because new symptoms and phenomena are analyzed on the basis of past knowledge. This situation exists in design for welding and, inasmuch as this field is new, we find undesirable environment is associated with this new form of production.

There are three predominant undesirable approaches used in design for welding. The first of these is designing to maintain the traditional physical structure as well as reproduction of functional activity of the various components by duplicating them as originally produced by other manufacturing environments, such as casting. The second is the designing of structures of extremely light weight without employing necessary precaution against localized buckling or nonelastic deformation to prevent loss of dimensional stability and vibration control. The third is designing where elements are produced only by fabricating parts entirely from plate, bar or shaped stock instead of using composite designs employing a combination of castings, forgings and pressings, with plates and shapes.

Today's age of specialization may inadvertently cause an individual to overlook the importance of associated influences. This fact is, to a great extent, responsible for employing the use of a specialized method where other forms of fabrication would be more economical or functional. A further characteristic responsible to the present age of specialization is a matter of minimizing the intricate details or importance of fields outside the specialized field of endeavor by the motivating individual. Consequently, a welding specialist may concentrate heavily on welding and overlook the importance of minimizing its use by other more economical methods of manufacture.

## Fundamentals of Design

Designers must be familiar with the equipment which is to be employed in producing the designs, and particularly the limitations or economical capabilities of this equipment and processes. The geometric or style lines for a product manufactured by conventional press brakes with their dies will differ from those produced by forming presses with their dies. Similarly, a product for job-shop application will differ in appearance if it

John Mikulak is associated with Worthington Corporation, Harrison, N. J.
Paper presented at 1958 AWS Annual Spring Meeting held in St. Louis, Mo., April 14-18.

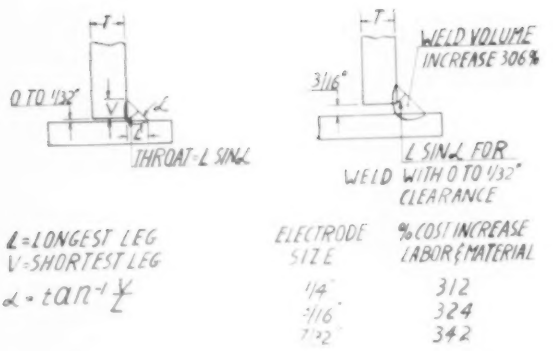



Fig. 1 Welding-cost increase caused by poor joint fit-up

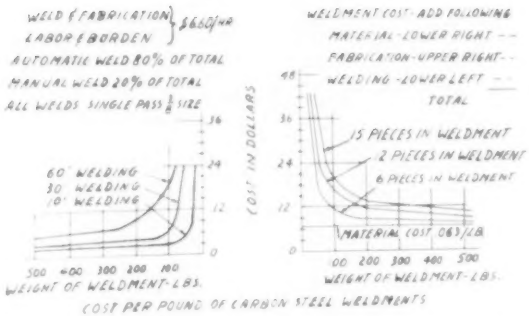

WELD & FABRICATION LABOR & BURDEN $6.60/HR
AUTOMATIC WELD 80% OF TOTAL
MANUAL WELD 20% OF TOTAL
ALL WELDS SINGLE PASS 1/4 SIZE
60° WELDING
30° WELDING
10° WELDING
COST IN DOLLARS
WEIGHT OF WELDMENT - LBS.
WELDMENT COST: ADD FOLLOWING
MATERIAL - LOWER RIGHT - -
FABRICATION - UPPER RIGHT - -
WELDING - LOWER LEFT ——
TOTAL
15 PIECES IN WELDMENT
2 PIECES IN WELDMENT
6 PIECES IN WELDMENT
MATERIAL COST .063/LB.
WEIGHT OF WELDMENT - LBS.
COST PER POUND OF CARBON STEEL WELDMENTS

Fig. 2 Increase in cost of fabrication with number of members and length of welds to produce the structure

is to be welded by automatic or semiautomatic welding processes in place of the usual manual arc-welding process. This is the result of the economics of shop application and stems from the characteristic behavior of the equipment and processes.

Full automatic operation is confined to regular configuration of either straight or circular joint geometry. Semiautomatic operation, in most cases, can be used on irregular joint configuration. However, in both instances the joint must have sufficient thermal capacity to keep the arc pool or weld metal from running or melting through the joint. Further, for high-quality results there must be a suitable balance of thermal capacity at both edges of the members being joined. In the case of the high-capacity submerged-arc processes, the joint must provide a sufficient trough to contain the flux material as well as the relatively large arc and slag pool, or otherwise special devices must be employed which introduce shop problems and special tooling.

While the manual arc-welding process is the most versatile from an application standpoint, the cost of application is generally considerably higher than obtained by automated processes and, further, the manual process possesses poorer quality control. In cases where the manual arc-welding process can be used to weld joints which possess poor joint characteristics for the automated processes, the cost is exceedingly high, as illustrated in Fig. 1. Consequently, the use of proper design and good fabricating tools and methods provide substantial returns in lower shop costs whether manual or automatic welding is used.

As illustrated in Fig. 2, the cost of welded fabrication increases with number of pieces which must be fabricated to produce the design. The figure also indicates a more obviously recognized cost variation with changes in the length and size of welds required. In the latter, it is assumed that single-pass welds are used equivalent to $\frac{1}{4}$-in. size fillet-weld area. Consequently, to minimize cost, the design should employ composite designs where bending, pressing, forgings, castings and extruded shapes are combined to produce a functional and economical design.

The composite design approach allows the use of the most desirable characteristics of many processes such as castings, forgings, extrusions, pressings, etc., as well as the use of the most desirable materials for the various elements of the design. In many instances, methods of manufacture such as casting or forging normally produce an inferior over-all result when attempting to produce a one-piece integral cast or forged unit. Welding can enhance the over-all product quality and decrease the shop cost by simplifying the members to be produced to eliminate or simplify shop problems and, at the same time, minimize equipment and tooling cost. This is illustrated in Fig. 3 where the compressor housing employs castings and plate to manufacture a higher-quality product with minimum shop cost, minimum weight and pattern equipment cost, as well as to eliminate scrap and extra work which was encountered in one-piece casting designs.

When creating designs, it is necessary to determine that each element and each line added to the drawing takes cognizance of the environment existing in the fabricating and welding shop. A properly designed weldment will generally produce a new approach and

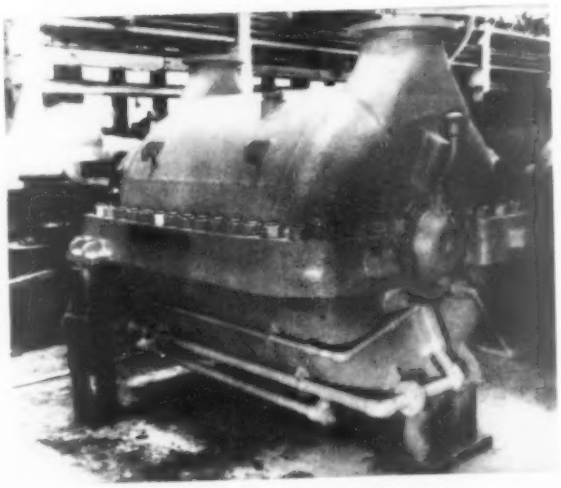


Fig. 3 Compressor housing using cast-weld design

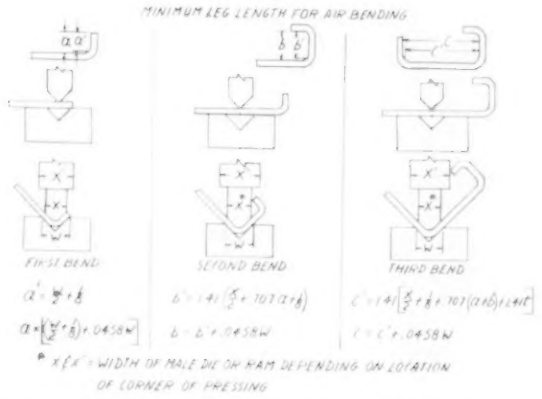

MINIMUM LEG LENGTH FOR AIR BENDING
FIRST BEND
SECOND BEND
THIRD BEND
a' = B/2 + t/8
a = (B/2 + t/8) + .0458W
b = 1.41 (B/2 + 10T (a/t))
b = b' + .0458W
c = 1.41 (B/2 + t/8 + 10T (a+b)/t + t/4)
c = c' + .0458W
* X & X' = WIDTH OF MALE DIE OR RAM DEPENDING ON LOCATION OF CORNER OF PRESSING

Fig. 4 Equations to determine minimum distance between adjacent joints

our critical inspection for appearance must expect this approach. Association with this "new look" will condition our taste for appearance to produce complete satisfaction if the functional ability and utility of the product are satisfactory. A poorly designed product for any form of manufacture is a deterrent to that industry as it inundates the good characteristics to the less than expertly informed.

## Bending

Perhaps the most important tool in the average job or weld fabrication shop is the press brake. Yet relatively few shops employ this equipment as an important cost-reducing operation. Much of the responsibility for this fact can be placed against the design and the engineering departments due to uneconomical requirements on the drawings.

Press brakes to be operated economically require designs where the press brake can be operated with minimum downtime for die changes, die adjustments or trimming operations. The practical limitations of designs depend on the general characteristics of the product to be manufactured. As an example, an offset die may be considered special to one manufacturer where for another it could be considered standard. As a consequence, the constants for determining limitations on design must be determined by each organization.

Comments are given in the following as a brief general guide for efficient use of press brakes.

1. Air bending is more practical than that with the closed dies. Air bending is generally more applicable on thicknesses $\frac{1}{4}$ in. or over.

2. Drawing should limit the radii used on the designs to one value, which for air bending would be $\frac{1}{32}$ of the female die opening. Female die opening selected should be 8 times the average material thickness used in general production. This will allow bending material of one type, such as open-hearth steel, from $\frac{1}{2}$ to $1\frac{1}{2}$ times the thickness used to determine the female die opening.

3. Most wide plates are finished cold (under 1000° F) and, as a result, the plate material obtains directional physical properties. Whenever possible, plate material should be ordered and cut so that the bends are made across the rolling grain of the material.

4. Whenever possible, double bends should be made to obtain most desirable accuracy.

Material Allowance for Air Bending and Inside Radius on 90° Bend

| MAT. | Rn / L | Rn / L | Rn / L | Rn / L | Rn / L | Rn / L | Rn / L |
|---|---|---|---|---|---|---|---|
| 1/16 | .109 / .174 | .1815 / .294 | .344 / .540 | | | | |
| 12 GA. | .133 / .208 | .211 / .331 | .367 / .576 | .523 / .821 | | | |
| 10 GA. | .139 / .2103 | .213 / .334 | .369 / .579 | .525 / .824 | | | |
| 3/16 | | .231 / .362 | .387 / .607 | .544 / .854 | .700 / 1.099 | | |
| 1/4 | | | .392 / .615 | .549 / .862 | .705 / 1.106 | .830 / 1.303 | |
| 3/8 | | | | .619 / .974 | .775 / 1.216 | .900 / 1.413 | 1.087 / 1.706 |
| 1/2 | | | | .669 / 1.05 | .825 / 1.295 | .950 / 1.491 | 1.137 / 1.785 |
| 5/8 | | | | .687 / 1.078 | .844 / 1.325 | .969 / 1.496 | 1.125 / 1.766 |
| 3/4 | | | | .694 / 1.089 | .850 / 1.336 | .975 / 1.533 | 1.162 / 1.824 |
| FEM. DIE SIZE | 1/2" | 1" | 2" | 3" | 4" | 5" | 6" |
| Ri | 5/64 | 5/32 | 5/16 | 15/32 | 5/8 | 3/4 | 15/16 |

Rn = radius to neutral axis
Ri = inside radius
L = allowance
L = 2π (Ri + Rn) 3/8
Rn = Ri + K
K = .5 up to 12 gauge material
K = .4 from 12 ga. to 1/4" material
K = .35 for 1/4" material
K = .3 for 1/2" material

Fig. 5 Table giving constants for determining length of material and distance to neutral fiber at bends. Note inside diameter constant for various metal thicknesses on one die opening

In Fig. 4, equations are shown to determine the minimum distances between bends when using conventional straight dies. When checking for dimension, it is necessary to determine whether the interference will be to the male die or the ram to select the proper value of dimension X.

Figure 5 provides data to assist in determining material requirement and bending layouts for a 90-deg bend as well as the radius ($R_n$) to the neutral axis at which the material requirement was obtained. The data were compiled by formula and checked in actual production on a hydraulic press brake with air bending. The chart also shows the radius to inside plate surfaces with various female die openings.

It is important that the edge of the plate from the first bend is sufficient to allow material to be supported properly. Further, it is not economical to trim the

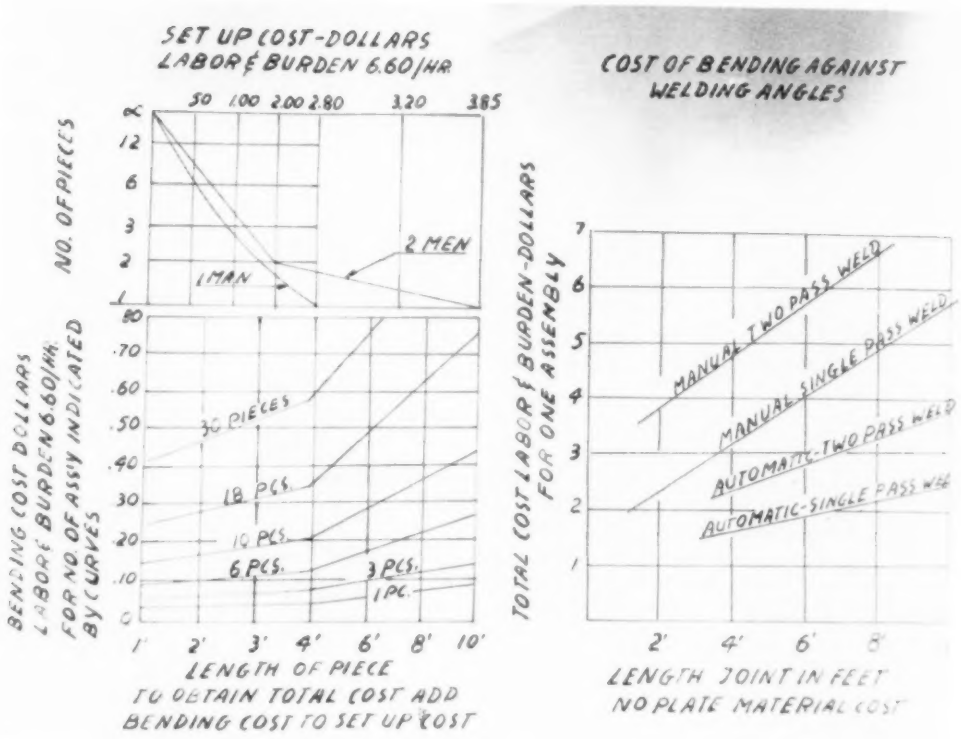



Fig. 6 Cost of labor and burden to bend versus cost of fabricating and welding plates

plate to provide lower flange heights and, consequently, for this purpose the female die opening should be made as narrow as possible. Where possible for most economical material requirement and, also, to obtain maximum precision on straightness of the flanged edge and flatness of the material, double bends should be used. A corrugation can be considered as a double bend whether made in one stroke or multiple strokes.

On an oxygen-cut edge, due to existence of high levels of residual stresses along the torch-cut edge, there results a nonlinear stress distribution of the horizontal shear, causing a greater plastic flow near the edge and, consequently, a nonlinear spring back to provide the resulting convex bow on the profile. This characteristic is not as predominant on sheared edges. The stress distribution after the first bend provides a greater value of horizontal shear in the middle of the plate and, consequently, a second bend in near proximity of the first reacts in an opposite manner to remove the bow obtained on the first bend. On long plates, nonlinear material upsetting results can be obtained due to nonuniform pressure caused by deflection in the ram and is especially noticeable when tonnage used nears the rated press capacity. Consequently, the shortest adequate machine width should be used.

Figure 6 illustrates the cost of labor and burden for fabricating plates and bending or welding plus the cost for welding material. From this illustration, it can be seen that substantial cost reduction can be made by bending over welding. It also indicates that it is important to minimize die changes.

Joints made by bending are also important from an appearance standpoint, as properly bent corners will attract much desirable highlight and therefore influence appearance. In designing, preference should be given to making outwardly located corners on the structure by bending and, further, vertical corners should be given preference to horizontal corners.

## Fabrication

Before injecting the welding operation in consideration for proper welding design, other important factors must be considered, such as method of fabricating material. Plate material for rectangular shapes can generally be sheared more rapidly and accurately up to $\frac{1}{2}$ in. thickness than by oxygen cutting. However, in some instances, where suitable facilities are not available for handling at the shear, large plates can be oxygen cut with less effort. For good welding fit-up, shearing over $\frac{1}{8}$-in. thick material is not recommended because of the distortion produced on the sheared edge, setting up of incipient cracks and, what is more, material over this thickness generally is prepared for welding by beveling, which ultimately requires the extra handling required in oxygen-cutting operations. Shearing on heavier materials can be economically used if plate preparation is performed by a machine plate edger. In this instance, stock should be allowed at

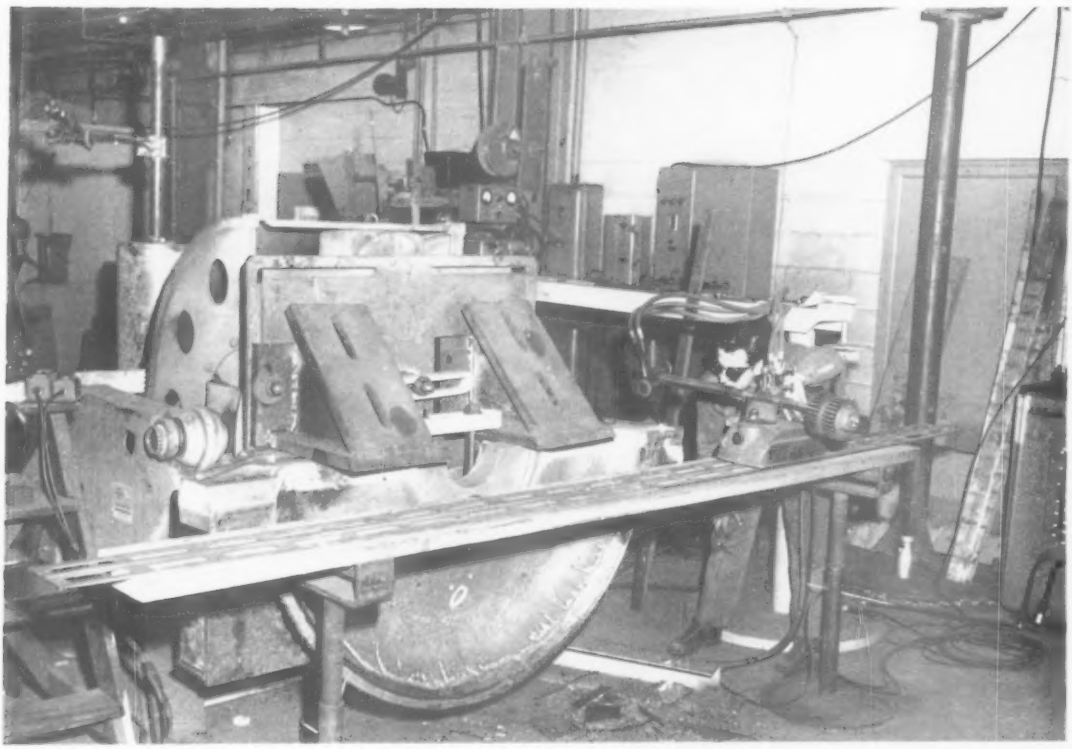


Fig. 7 Trimming end housing casting for compressor in Fig. 3 by oxygen cutting using weld positioner and portable cutting equipment

the root of joint or face of land to remove possible incipient cracks resulting from the shear operation.

Where castings, forgings or pressings are used as components, they should be designed so that they can be sheared, machined or torch cut to provide proper fit-up. On castings or forgings, it is necessary that these members be designed with a flange of approximately the same thickness as the mating members to facilitate the fabrication operation.

Foundry practice may result in inability to control closely the chemistry and the grain size of the material required for weldable grade; consequently, all casting heats (1) should be checked for analysis of chemical residual elements as well as basic chemistry, and (2) should be specified of welding-grade structure which must be controlled by heat treatment. In many instances, inspection of castings by radiology will provide economy in the over-all manufacturing cost by minimizing extra repair work or scrapped weldments. Figure 7 illustrates the trimming operation of an end casting for a compressor housing while Fig. 8 illustrates oxygen cutting of the housing plate which fits to the castings in the previous illustration. This procedure eliminates most of the shop tolerance ordinarily incurred by rolling not being square to intended axis. The simple tooling shown assures good fit-up and, thus, results in economy by minimizing fitting and welding costs, to overcome the extra handling required.

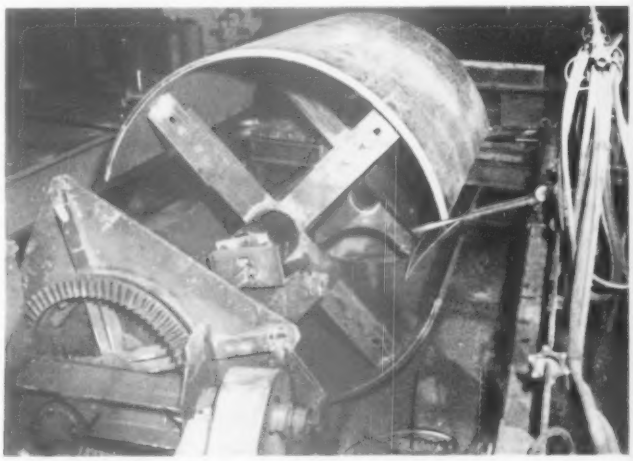


Fig. 8 Trimming housing plate by oxygen cutting for compressor housing after plate is rolled, which assures good fit-up at welded joints

Fabricating shops are prone to practices considered satisfactory in riveted and bolted designs. Fabrication for welded construction must be of higher quality and, therefore, the equipment must be maintained to produce accurate precision. Cutting-blade edges, blade clearance and rake on shears must be watched. Hold-down equipment must be adequate and must contact work uniformly; further, material must be gaged by positive stops to assure accurate results.

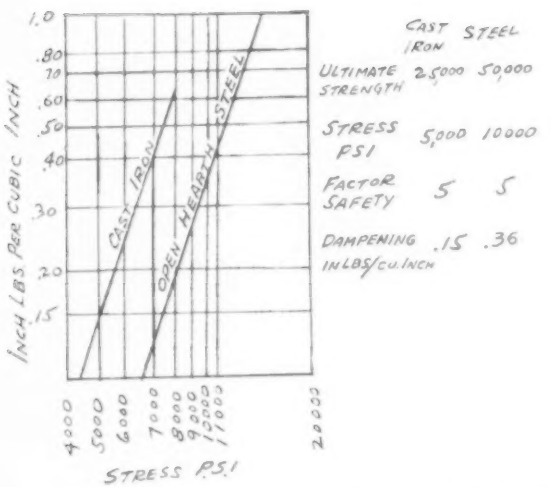



Fig. 9 Vibration dampening on cast iron and steel shown as a function of membrane stress

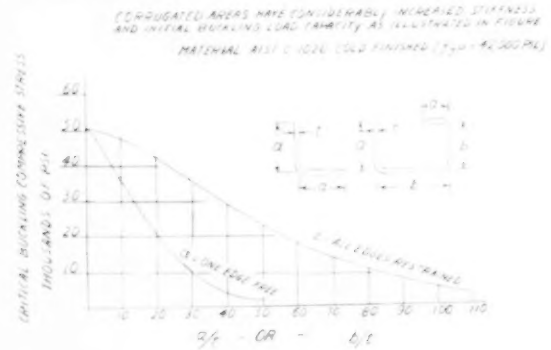



Fig. 10 Critical buckling stress for free and supported edges

Bending rolls must be accurately aligned, and bearing and jib clearance must be maintained so that rolls stay aligned as plate is fed into rolls. Provision should be made to feed material into the rolls square to provide cylindrical members. In Fig. 8 the simple fixtures allow preparing the center plate of the compressor housing after the shell is rolled to assure precision fit to the casting shown in Fig. 3.

Similar precautions should be taken on all equipment in the fabrication shop so that good fit-up is obtained. Good fit-up will assure minimum setup and weld costs and, also, uniform control on distortion. In addition, good fit-up assures proper thermal capacity at the joint to allow use of intended welding processes at optimum current densities to provide control in joint metallurgy and physical properties.

## Joint Design

For economical manufacturing, the problem of welding is still not being considered because the selection of types of material, material sections and shape configurations, as well as vital factors such as joint design, joint work clearance and joint accessibility, determine to a great degree the shop's ability to produce an optimum cost savings.

As stated by other authors, the material used in the design must be stressed to the highest safe value. This improves the functionability not only from a manufacturing standpoint but also from a vibration and noise standpoint. Obviously, natural frequencies of the various members, as well as the parent and supporting structures, must be avoided. Large flat diaphragming areas must be avoided, as this type of vibrating member is difficult to control because of the many nodes of vibration which can be set up.

In present weld-design manufacture, the material cost can be between 30 and 80% of the total manufacturing cost. Products employing low-carbon steels, with material cost over 45% of total manufacturing cost, do not give satisfactory manufacturing economy since limited ability is available to provide shop-burden expense absorption while attempting to maintain a competitive-market position. Except in certain industries where light weight is essential, relatively little is being done to improve the light-weight construction which ordinary low-carbon steels are capable of producing.

The problem of light-weight construction requires that the industry demand improvement and more tonnage of materials produced by newer methods of fabrication such as extrusions, pressure-spinning, shear-flow forming, stretch forming, etc. Highly productive job-type equipment should be developed such as the "structural iron worker" with features that provide for more precision results than presently obtained. Producers of heavy rolling and forming equipment have accomplished relatively little to provide equipment features which improve performance from a cost and precision standpoint.

Cast iron is erroneously considered a better material for machinery construction because its heterogeneous structure is supposed to dampen vibration energy. It has been shown by M. Kronenberg, P. Maker and E. Dix in their paper on "Controlling Vibration in Welded Machines" that materials to absorb energy must have sufficient time between impulses to decay the residual energy.

Cast iron is reputed to possess ten times the energy absorption of steel. This statement must be qualified for static conditions with a single impulse or impulses of low frequency, allowing sufficient time for the cast iron to dampen fully the energy of each impulse before an additional impulse is applied. Due to lack of perfect elasticity in materials, when an energy impulse is removed, and the stress on the material decreases to a zero value, there still exists a residual strain on the material in the structure. This strain must be removed by decay and requires a time interval for the phenomenon to occur and therefore, results in a typical hysterisis loop. The area of this loop approximately represents the value of the energy dampening. In machinery the frequency of energy application is too great for this phenomenon to occur and consequently,

under these circumstances, cast iron becomes an efficient media for energy transmission.

The main objective in design from a vibration or noise consideration is to stress materials to maximum allowable working stress and thereby minimize overburdening production cost by excessive material and the coincident labor factor.

Figure 9 illustrates that a comparison of cast iron to steel, applying the same factor safety of 5 on minimum tensile strength, produces a large increase in dampening capacity for steel.

In designing with steel, it is necessary to exercise precaution on the use of excessively thin sections without providing corrective design measures to avoid vibration and loss of dimension stability. Cast iron automatically provides a control over this factor because the pouring problems in the foundry require use of heavy walls and shrinkage stresses require considerable amount of ribbing.

Light weight in steel design can be accomplished by corrugating or making the material rigid against localized buckling, as brought out in the following discussion.

Figure 10 illustrates the increased critical buckling-stress capabilities of a member with supported edges over the conventional unsupported edges. As the critical buckling stress is increased, the material section can be decreased, which minimizes the material cost and, in many cases, allows the manufacturer to provide for more economy in fabrication and welding cost, as well as an opportunity to provide more desirable shop-burden absorption capabilities. Corrugation can allow a reduction of material thickness as much as 60% over that normally used in conventional steel design.

Figure 11 demonstrates that the load to produce the same buckling stress can be increased by the use of supported edges. It can be seen that the safe load is increased to 850% with the supported edge. To obtain the same buckling resistance with a free edge, the material thickness must be increased 330%, as shown in Fig. 12.

To obtain the values shown in Figs. 11 and 12, the specimens were precision machined to exactly the same height and then placed between the carefully machined platens and loaded by a hydraulic press using sensitive meters as well as electronic strain gage equipment.

The conventional use of rectangular or transverse stiffening ribs produces very poor utilization of material against torsional resistance. Torsional loads can be set up by unequally applied load caused by deflection, unbalanced loads or unbalanced bending stresses. Figures 13 and 14 illustrate the deflection for members of the same material thickness and dimension, under identical loads, where one is ribbed by diagonal bracing and the other by rectangular bracing. There is a 100% increase in torsional deflection with the rectangular ribs. Further, a moderate increase in material thickness of the longitudinal members without an increase in transverse ribbing does little to effect a perceptible change in torsional resistance.

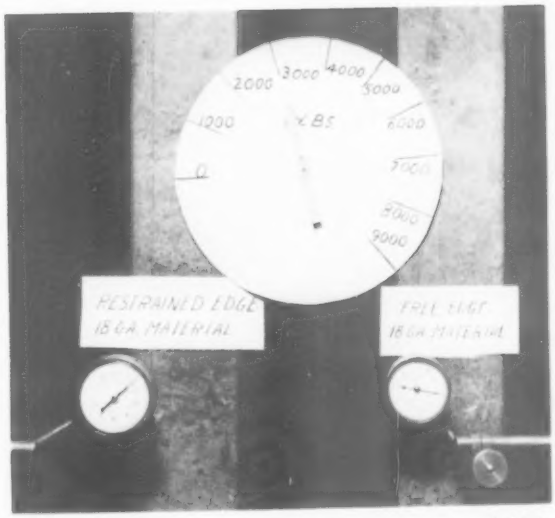



Fig. 11 Buckling of two members of same size and thickness under identical load, one having a supported edge and the other a free edge

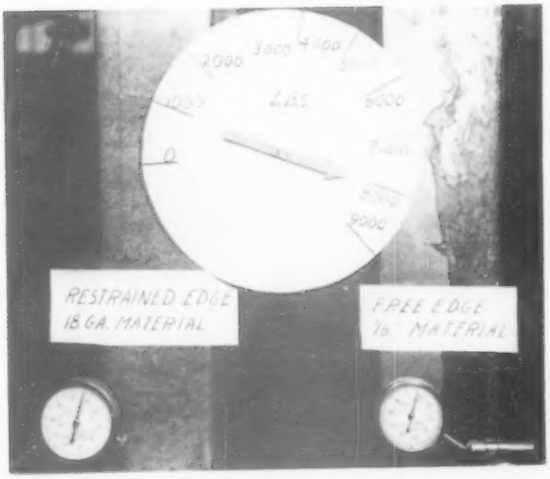



Fig. 12 Buckling of two members of different thickness but same size. Thicker (1/8 in.) member has free edge and thinner member has a supported edge

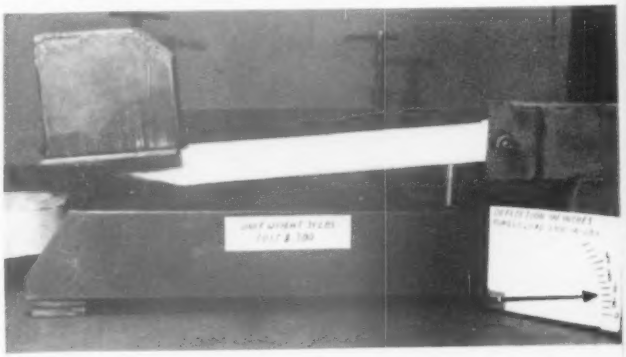


Fig. 13 Torsional deflection of bedplate with diagonal bracing (approx. 1/16 in.)

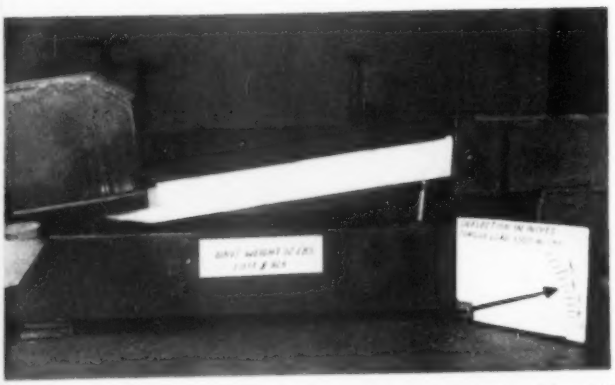


Fig. 14 Torsional deflection of similar bedplate (Fig. 16) with rectangular bracing (approx. $\frac{3}{8}$ in.) increased 100%

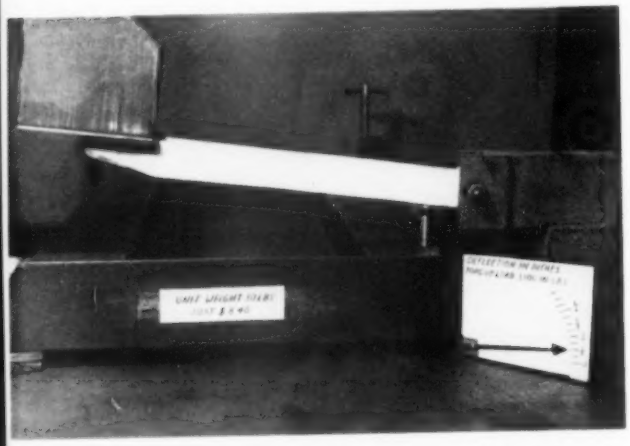


Fig. 15 Torsional deflection of light weight bedplate design with shear web (approx. $\frac{1}{4}$ in.) decreased 33 and 66% (respectively) over designs in Fig. 13 and Fig. 14

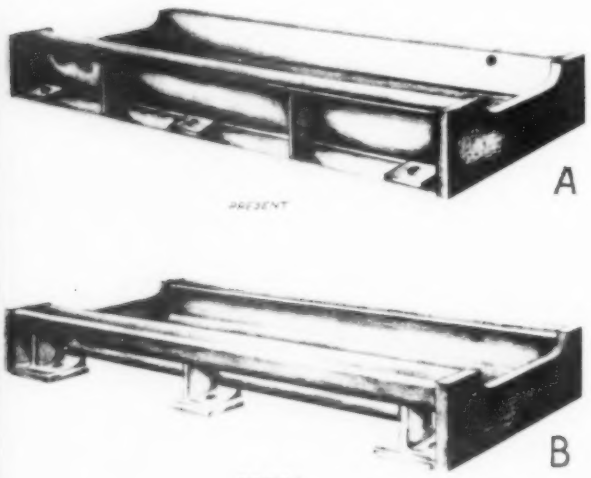



Fig. 16 Illustration of design analogy for vertical pump crankcase housing

(A) Conventional design—17 pieces; (B) light-weight design—11 pieces.

From Fig. 14 it may be concluded that material thickness can be reduced to a marked degree with diagonal bracing for the same deflection and can effect substantial manufacturing cost reductions.

The bedplates illustrated in Figs. 13 and 14 used the conventional design approach where rolled sections are used for the longitudinal member with rectangular or diagonal transverse reinforcing ribs. In Fig. 15 a new design approach is used which incorporates all of the desirable features of Fig. 14 but, in addition, the one-piece design increases the transverse and longitudinal stiffness by employing the horizontal shear web to increase rigidity and, thereby, decrease shop costs. In Fig. 15 the deflection is reduced 33%, with a 66% reduction in material thickness as well as the number of pieces required to produce the design to decrease shop cost.

An illustration of the latter type of design is given in the lower-half pump-crankcase housing shown in Fig. 16. The original design (Fig. 16a) was quite conventional in general approach in that many pieces were used and all parts were produced from plate. The revised design, Fig. 16b, used castings for feet and employed only 11 pieces against 17 for the original design; this not only decreased fabrication and welding costs 26%, but also reduced machining cost and improved appearance.

The welded machine design industry has accomplished very little in producing light-weight designs, as shown in Fig. 17, for a gear train housing. Previous cast designs employed $\frac{1}{2}$ to $\frac{5}{8}$-in. wall thickness whereas these units employed No. 11 gage material. Conventional steel design employed $\frac{1}{4}$-in. material with many ribs and gussets. Corrugations such as shown in Fig. 17 can be made with standard press-brake dies. Figure 18 illustrates the die setup for producing this type of corrugation. The draw bars are set so that they can contact the material after the corrugation is set approximately 5 to 10 degrees. At this time, the draw pads contact and hold the material as the dies set the material in the corrugation.

For very light materials, sheet stock of a rigidized design can be purchased as a commercial product. For large-quantity production, corrugation can be produced more economically by rolling with special dies.

Material which has been corrugated can be rolled

Fig. 17 Light-weight gear-train housing employing rigidized members (11 gage thickness)

in standard forming machines by use of steel spacers or rubber filler sheets. The use of this operation produces a very economical setup and improves appearance as well as cost. The actual rolling operation with spacers is illustrated in Fig. 19. The spacer thickness, whether of steel or bolting, should be approximately the same or somewhat greater than the height of the corrugation.

Figure 20 illustrates a bedplate which must have controlling surface at two or more positions on several members whose mounting dimensions are at different elevations. By using two subassemblies which can be aligned after major welding operations have been completed, thus minimizing fabrication tolerances and welding distortion, the material thickness can be reduced by providing minimum stock for machining. Heavy bolting pads are required primarily for excessive machining allowance and tapping stock. The use of welding studs can eliminate this requirement. When welding studs are used, it is important that the welding equipment be in good operating condition and that the power supply is designed for the application. Conventional generators generally do not provide adequate and uniform energy input for producing high-quality welds. It is also essential that studs be kept clean and dry; in addition, they must be inspected to determine whether they are provided with proper shielding. To locate and position studs accurately for proper assembly, they should be applied with locating fixtures equivalent in design to those used for conventional drilling.

Figure 21 shows a bedplate for a lower half of a vertical reciprocating-pump crankcase where $\frac{3}{8}$-in. stock is used. These units are up to 76 in. long and only $\frac{1}{16}$ in. was allowed for finish. Welding studs are used for bolting to upper half.

## Welding

If optimum results are obtained in design and fabrication of the various elements, there is relatively little

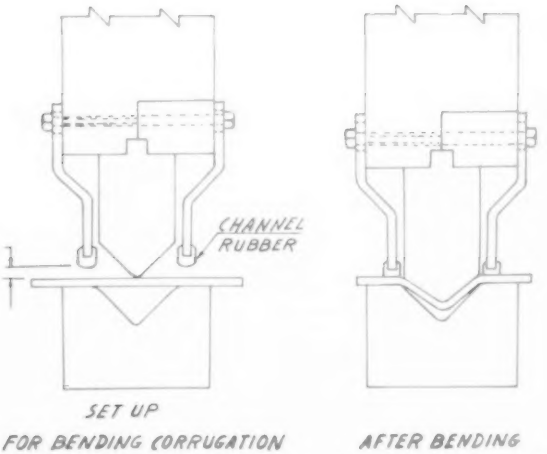



Fig. 18 Conventional die setup for corrugating metal using standard press-brake dies and employing simple draw pads

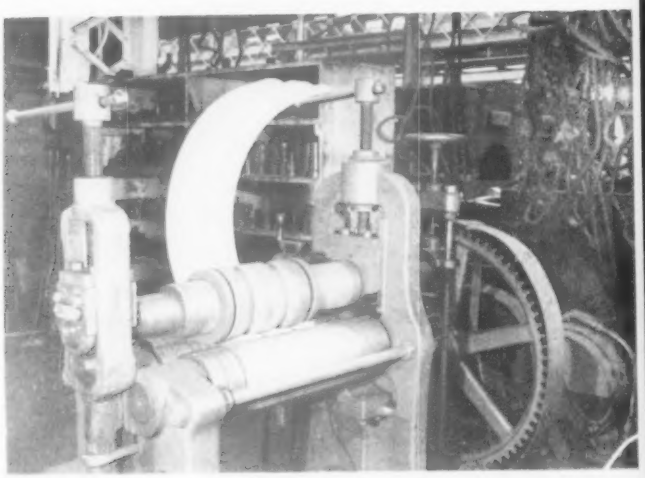


Fig. 19 Rolling operation on a corrugated member showing spacers on top roll of standard initial pinch-type roll

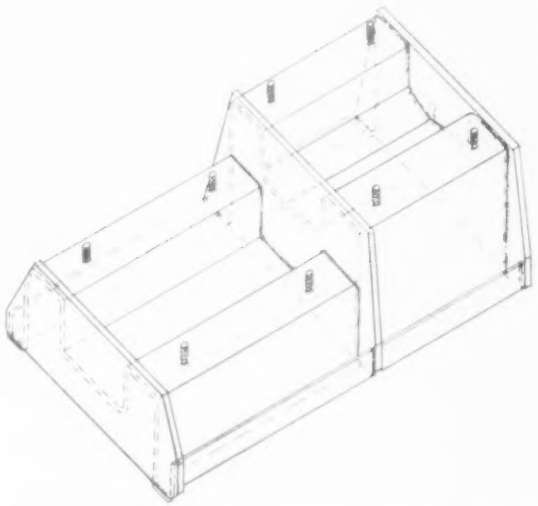


Fig. 20 Boiler feed-pump bedplate produced from two fabricated and welded subassemblies to provide precision alignment to minimize material allowance for machining

to be concerned with in the actual welding operation which is to be scrutinized at this time. There still exists an erroneous school of thought that manual welding operations can produce higher metallurgical and physical properties than automated operations. This impression is contrary to results being obtained, particularly for open-hearth steel. Automation in welding not only improves properties of the material at and adjacent to the joint, but also provides greater reliability in quality with substantial manufacturing-cost reductions.

Designs should be created with automation in mind. Thus, the joints must be made accessible, which, in some instances, may require that designs be produced by using subassemblies. These subassemblies should be designed so that, where possible, only one controlling surface is contained in one subassembly. With the

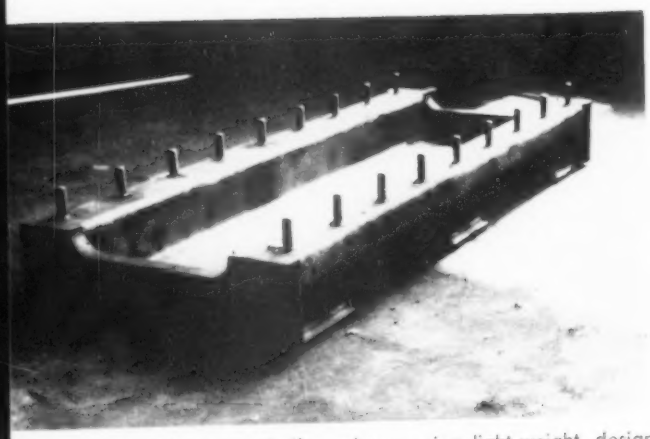


Fig. 21 Lower half crankcase using light-weight design principle of minimum number of elements, linear inches weld, machining allowance and machining cost

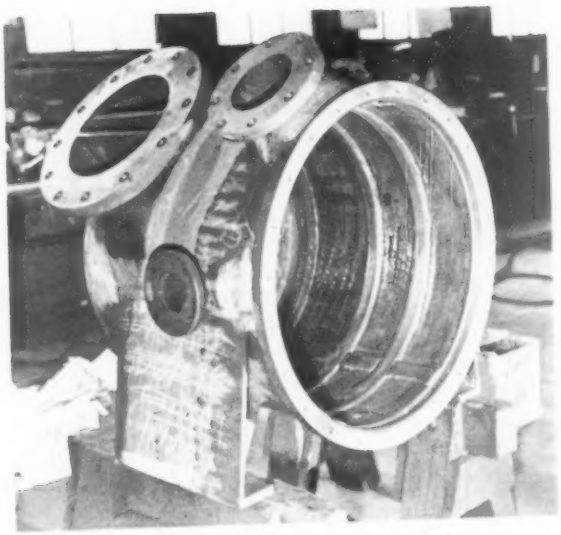


Fig. 22 Compressor case using composite-design (castings, pressing, bars and plate) nozzle added after all circumferential welding has been completed to provide for automatic welding and minimum handling

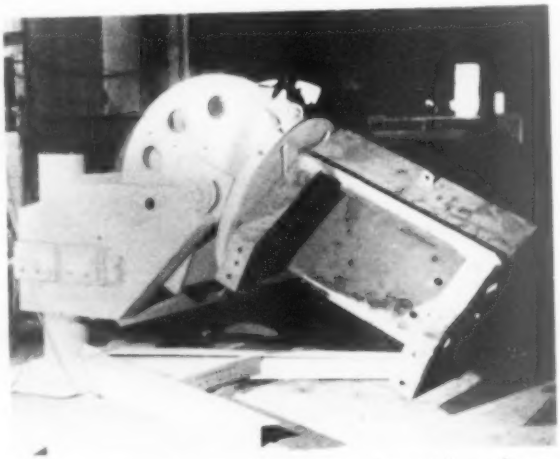


Fig. 23 Typical design which allows the welding of complete subassembly or main assembly in one setup operation

latter approach, if the design is made so that simple means are provided to make adjustments between subassemblies, machining costs and material costs can be reduced. This is illustrated in Fig. 22, where the complete shell is produced as a cylinder and only the bore is important. After this operation, the rough-machined nozzles are laid out, openings cut and the nozzles added, using the bore as a register. Later, the supporting feet are added so that the bore and the nozzles can be used as a register. Such a procedure allows the major share of welding to be done automatically using a standard set of turning rolls. Leaving the feet off while the nozzles are being welded, allows easier positioning of joints at nozzles for welding. If the feet were added before the nozzles were welded, positioning would have to be accomplished by crane or, as more commonly done, by large welding positioners instead of simple turning rolls.

Handling in the shop must be kept to a minimum. Design, whenever possible, should allow the welding of the complete subassembly or main assembly in one setup operation, as illustrated in Fig. 23. On this frame for a welding positioner, one set-up operation on the positioner allows the welding of all joints except two, the latter being made in a horizontal position after part is removed from table.

Another application is shown in the lead photograph where, with one set-up operation, using two positions of the positioner table allowed complete manufacture of the drum head for a concrete mixing drum.

As pointed out by others, it is important that weld be checked for correct size. For example, a $\frac{1}{4}$-in. fillet weld, while only 20% smaller in dimension than a $\frac{5}{16}$-in. fillet, requires 36% less weld metal and is approximately 21% less costly to produce. In addition, the smaller weld deposit produces less distortion and lower peak values of residual stress. Caution must be exercised in fatigue loading, however, because small fillet welds are apt to produce higher peak values of applied stress due to the greater stress concentration caused by their geometry.

In most instances, lap joints produce the lowest setup and weld lower cost but, unfortunately, in most instances this type of joint is undesirable from a stress capacity and application standpoint. Where most desirable weld and joint strengths are required, a double-vee or double-"U" joint should be specified. The single-vee or single-"U" joints can also be used, but the larger weld deposit sets up higher peak values of residual stress and also produces greater distortion. Single-bevel or "J" and double-bevel or double-"J" welds are likely to produce poor joint properties at the root of joint; they should not be used for critical stressed joints.

For statically loaded assemblies or for secondary members on other assemblies where stresses are of low order, completely penetrated joints may not be

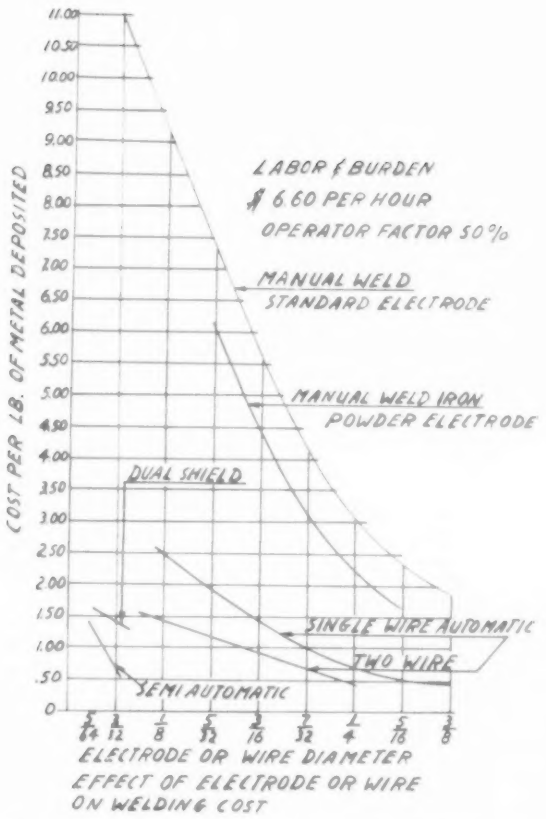



Fig. 24 Cost of deposited weld metal using various processes and diameters of electrodes or weld wire

required and weld would be applied to satisfy only the actual strength requirements.

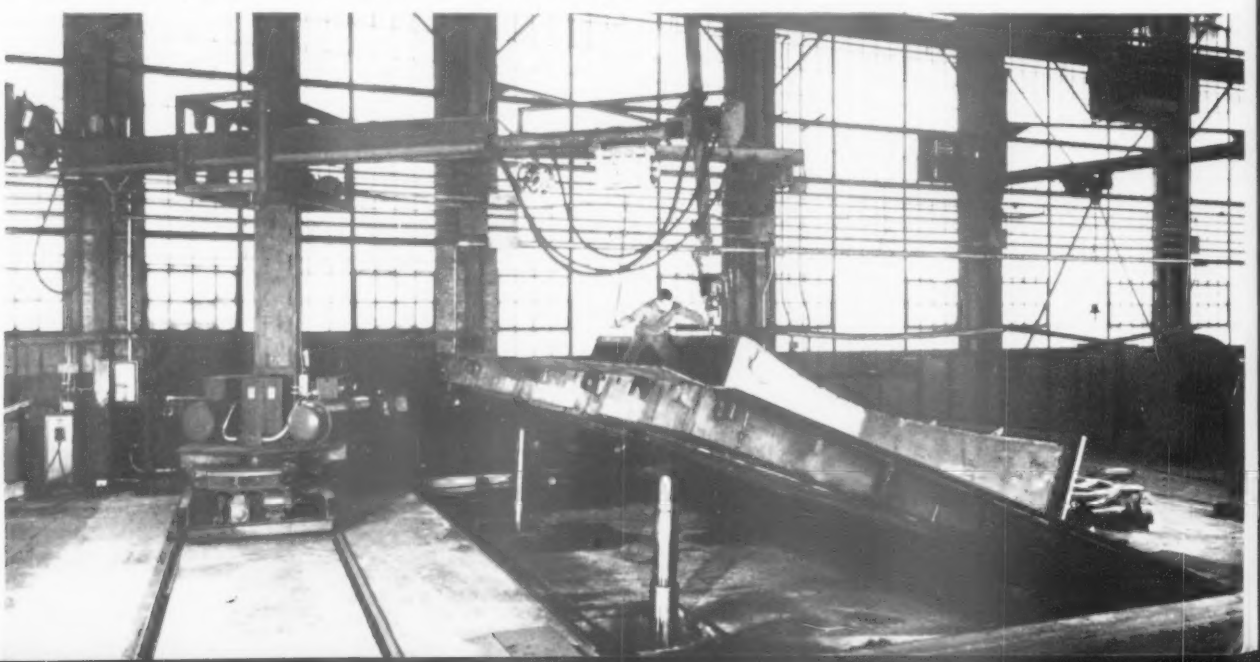
Automatic welding provides great opportunity to decrease welding costs and distortion as well as to improve distortion problems. Figure 24 illustrates the cost per pound to deposited weld metal using various welding processes, and indicates that there are areas where several processes can be used; selection would depend on factors such as type of material, joint design, welding position, geometry of joints, etc. Assuming that the proper joint has been selected, automation is a matter of versatility in tooling. To apply automatic welding, the equipment and the welding head must fulfill joint conditions such as shown in Fig. 25, where the joint is traced by a wheel directly in front of the arc. The head has vertical and horizontal movement to hold welding wires in the same relative position in the joint, even if the plane of the joint profile is not in line with travel direction of boom or carriage. With this type of tooling, it is not necessary to line up assemblies accurately for welding; therefore, it constitutes efficient job-shop equipment.

Design for welding is complicated by many far-reaching technical and nontechnical considerations, including stress analysis and metallurgy, which have not been discussed. It is imperative that the welding industry, with the assistance of the proper regulatory bodies, such as the American Welding Society, stress this importance to educators and educational institutions so that proper curricula can be made available to interested young men. There exists a definite need for promoting welding to the general public, thereby creating a tremendous influence for increased activity in our field and, what is more, instilling a desire in young men to be educated for a place in the welding industry.

*Acknowledgment*

The author wishes to express gratitude to Worthington Corporation for allowing the use of the included illustrations, which were all taken from various studies in the various works of its organization.

Fig. 25 Automatic welding equipment for job-shop application. Welding head provided with means to track joint and table can be tilted in any one of four positions



# INERT GASES FOR CONTROLLED-ATMOSPHERE PROCESSES

*Effective application of argon or helium for controlled-atmospheric processes depends upon utilization of appropriate purging techniques for chambers or furnaces*

BY E. F. GORMAN

## Introduction

Controlled-atmosphere processes are used extensively for producing or fabricating metals such as titanium and zirconium. The four major impurities encountered in these operations are oxygen, nitrogen, hydrogen and moisture. Estimates on the tolerable limits of these impurities have ranged from a low of about 10 parts per million for each specific impurity to a high of about 500 parts per million for their combined total. Lacking exact specifications, the general trend has been to provide atmospheres of the maximum purity obtainable.

In practice, the use of helium or argon as inert atmospheres for chambers or furnaces has sometimes been accompanied by product contamination. The user has often concluded that the purity of the inert gas, *as it comes from the supplier*, was the source of difficulties. As described here, however, contamination pickup on the part of helium or argon generally occurs as the gas is used for purging. Accordingly, contamination can be prevented through an understanding of how argon and helium are supplied, by following certain precautions regarding the construction and use of inert-gas distribution piping systems, and by careful regard to purging techniques and their limitations. When properly used, argon and helium can provide the desired protection against product contamination.

## Commercial Argon and Helium

### *Available Forms*

Argon is obtained from the atmosphere by liquefaction. Following refinement, it is transported as a gas in cylinders or as a liquid at temperatures slightly below −300° F. The cylinders in which argon gas is shipped are filled at a pressure of 2200 psi, although one size of cylinder is now also filled at a pressure of 2640 psi. As shown in Fig. 1, cylinders are available to hold as little as 78 cu ft and as much as 330 cu ft of argon.* For most controlled-atmosphere processes, the larger size cylinders are preferred.

When it is impractical to supply argon in cylinders because of large-scale demand requirements, argon is shipped in bulk as a liquid in either trailer trucks or railroad tank cars. As may be inferred from Fig. 2, bulk shipment results in greatly reduced transportation costs. Argon can be pumped as a liquid into storage facilities on the property of users, or it can be converted into its gaseous state and then stored in receivers from which it is piped to points of use. A recent innovation for shipping argon is illustrated in Fig. 3 which shows a new type of cylinder containing liquid argon. This unit is handled in much the same manner as are cylinders containing gas. Its liquid argon contents are, however, equivalent to 3000 cu ft of gas.

The production of helium is unlike the production of argon in that helium is obtained from natural gas by stripping off helium fractions. The helium is then refined and transported as a gas. All helium is produced by the U. S. Bureau of Mines at plants located in Texas, Kansas and New Mexico. The Bureau of Mines sup-

* All volumes in this paper measured at 70° F and 14.7 psi absolute.

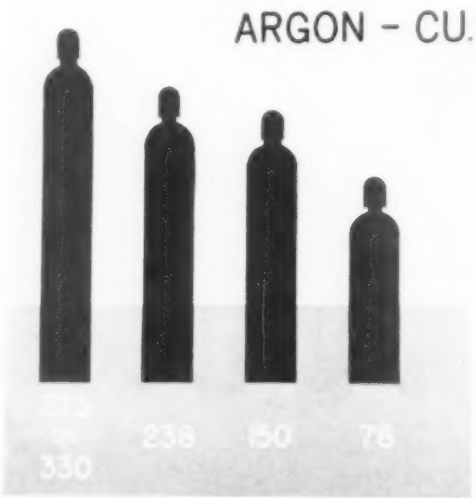



Fig. 1 Typical argon-cylinder capacities

E. F. Gorman is development engineer of the Linde Co., Newark, N. J.
Paper presented at AEC Welding Committee meeting held on Nov. 8, 1957, in Chicago, in connection with annual ASM meeting.

plies helium to government agencies directly; the balance is purchased largely by suppliers of industrial gases for resale to industry.

As supplied to the user, helium comes in cylinders which are filled at a pressure of 2200 psi with 235 cu ft of gas. When helium requirements are large enough to render the use of cylinders impractical, special high pressure trucks and railroad tank cars are then employed for bulk shipments of helium in gaseous form.

### Purity

Argon is commercially available in a wide range of purities. Impurities in argon include oxygen, nitrogen and water vapor with hydrogen- and carbon-bearing gases being present to a lesser extent. Table 1 compares the general distribution of these impurities for cylinder gas, bulk gas and bulk liquid.

It can be seen that there is a progressive improvement in argon purity levels as handling requirements for the gas are reduced. The differences are primarily due to varying degrees of air and moisture pickup as the gas is transferred from producer pipe lines to cylinders or other containers. During the past several years, the purity of argon has been constantly increased. In addition, procedures for cleaning, drying and filling cylinders and bulk containers have been greatly improved. It is now possible to obtain argon in cylinders and in bulk at a guaranteed minimum purity of 99.995%. At this level, the total impurity content is guaranteed to be not over 50 parts per million. In practice, continuing analysis has revealed that this argon in liquid form has an average total impurity of about 25 ppm or a purity level of 99.9975%.

In order to produce argon to this specification consistently, it was necessary to develop special gas-analysis methods since there are no instruments commercially available for directly and accurately detecting the separate impurities at extremely low levels. These methods involve concentration of the separate impurities in order to get enough of each for measurement. Special instrumentation is used at gas-producing and at filling stations, with even more precise devices at the quality-control laboratory.

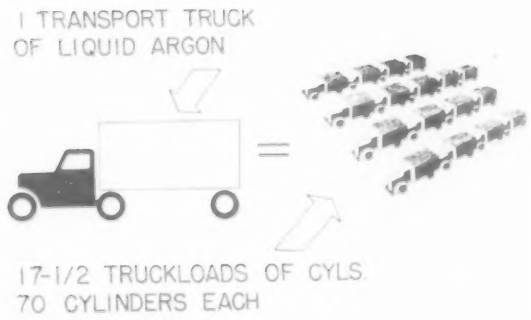



Fig. 2 Equivalent gas-cylinder capacity of transport trailer truck for liquid argon

Table 1—Comparison of Impurities in Cylinder and Bulk Argon

| | *Cylinder gas* | *Bulk gas* | *Bulk liquid* |
|---|---|---|---|
| $O_2$ | Some | Less | Least |
| $N_2$ | Some | Less | Least |
| $H_2O$ | Some | Less | Least |
| $H_2$ | <0 | <0 | <0 |
| $H_xC_y$ | Slight | Slight | Slight |
| CO | Slight | Slight | Slight |

Fig. 3 Liquid argon cylinder

Helium produced by the Bureau of Mines has a purity exceeding 99.995%, before storage in transport containers and is designated Grade A quality; the helium distributed by commercial gas suppliers is sometimes referred to as Grade XX. Residual impurities in helium include hydrogen, which is generally kept to a level below ten parts per million. Other impurities include oxygen-, nitrogen-, moisture- and carbon-bearing gases. While the analyses of helium going out from the helium plants are not guaranteed, precautions are taken to eliminate impurities so far as possible.

All Bureau of Mines shipping containers used in Grade-A helium service are thoroughly cleaned and purged before being placed in this service. Privately owned cylinders, however, are purged but not cleaned unless this is specifically requested by the owner. When cylinder helium of the maximum available purity is desired, it should be obtained from the Amarillo plant in Texas where special conditioning facilities have been installed.

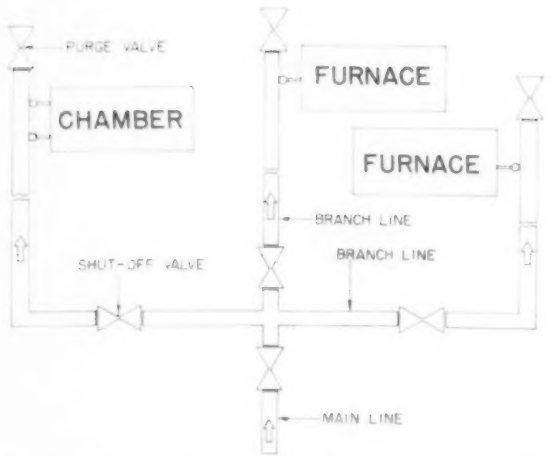



Fig. 4 Layout of distribution piping system for inert gases

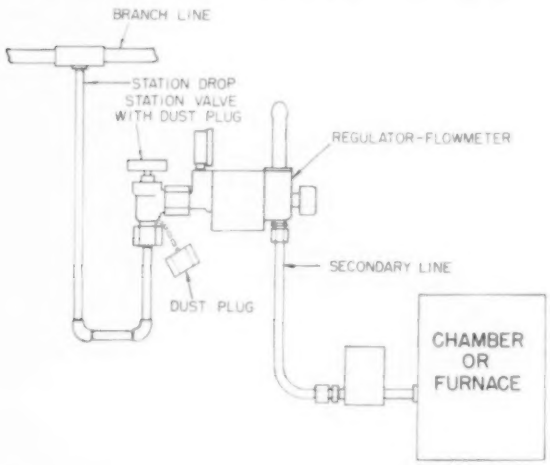



Fig. 5 Station outlet for inert-gas distribution piping system

## Inert-Gas Distribution Piping Systems

### *Problems of In-Plant Distribution*

As supplied to the user, the purity of argon and helium meets the requirements of most controlled-atmosphere processes. The user is then confronted with two problems—distributing the gas in a convenient manner to points of use throughout the plant and distributing the gas in a manner which will avoid contaminator pickup.

When gas volume requirements are small, individual argon or helium cylinders can be used directly at the chamber or furnace where an inert atmosphere is needed. Ordinarily, however, it will be more convenient to install an inert-gas distribution system consisting of four elements:

1. Source of inert-gas supply.
2. Main and branch line piping, with valves, leading from the source of gas supply to station outlets near points of use—illustrated in Fig. 4.
3. Station outlets including gas-flow equipment near points of use—see Fig. 5.
4. Secondary lines leading from station outlet gas-flow equipment to inert-gas atmosphere process chambers or furnaces—also included in Fig. 5.

### *Sources of Contamination*

As described here, the installation and use of inert-gas distribution piping systems call for an understanding of the means by which inert-gas contamination can and does occur when certain precautions are not followed. Thus, if it occurs, contamination of the inert gas in a distribution piping system can be attributed to one or more of three basic causes—outgassing, air leakage and improper purging of the system prior to use.

As one of these three basic causes, outgassing is the evolution of gases and vapors from materials by virtue

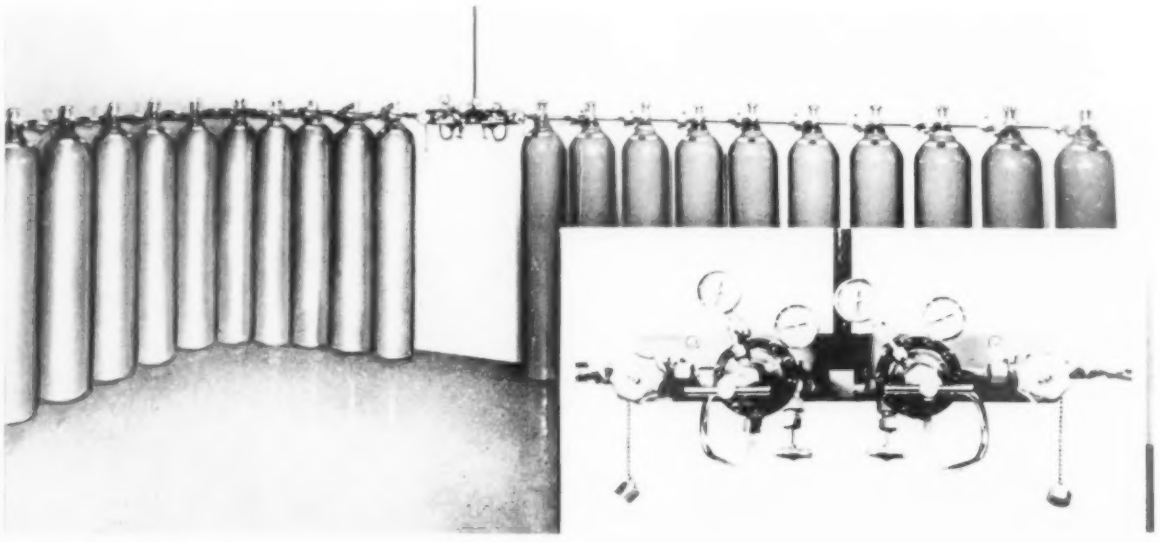


Fig. 6 Manifolded cylinders

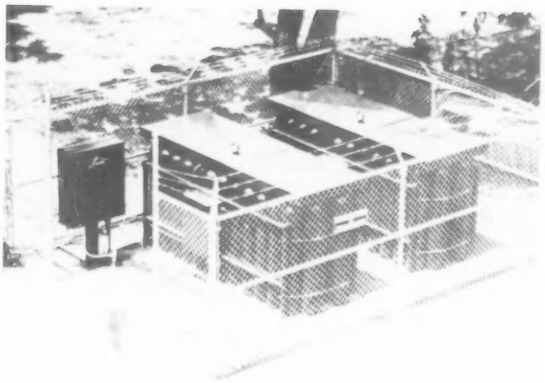


Fig. 7 Permanent bulk-storage installation for argon gas

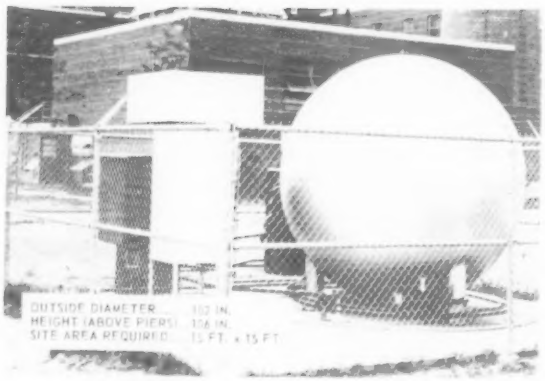



Fig. 8 Permanent bulk-storage installation for liquid argon

of differences in partial pressures. This action proceeds in much the same manner as water evaporating into dry air. The volatile components in grease and oil, if left on the interior surfaces of pipe, fittings and valves, will exert their own partial pressures and hence will contaminate the inert gas in a piping system. The rate at which these contaminators evolve into the inert gas will be determined by such factors as the temperature and total pressure of the system, the partial pressures of the contaminators and the amount of exposure of the contaminating surface area. The level of contamination will increase until such time as equilibrium is reached or the source of contamination is exhausted.

Gaseous contaminators such as oxygen and nitrogen are adsorbed on metal surfaces in small quantities and can be disposed of in a relatively short time by good purging procedures. On the other hand, these gases can be absorbed in large quantities by grease, oil, moisture, pipe dope or rust.

Water vapor is the most difficult contaminator to remove from a distribution piping system. All surfaces of newly installed systems will contain a film of adsorbed water vapor. Rubber and most plastic materials contain appreciable quantities of moisture by virtue of their adsorption and absorption characteristics. Rubber picks up moisture from the atmosphere and from the steam-curing process; plastics absorb moisture from air and contain various plasticizers which can also be a source of contamination.

Leakage into a gas system can occur in two ways — by the permeation of air and moisture through porous structures and by air diffusion through faulty joints. Rubber and many plastics transmit gaseous contaminators to varying degrees, depending upon the porosity of their structures. Leakage at joints is most often encountered at fittings not specifically designed for inert-gas service or when the joint-sealing surfaces are scored or excessively deformed. Pressurized lines, which have been shut off during periods of nonuse, can become contaminated with air and moisture through leakage.

It is impossible to purge a gas line properly when there are blind ends or serious constrictions in gas passages. Thus, in designing and installing a distribution piping system, consideration must be given to the problem of purging the line before it is put into service. A dirt leg may be desirable in a water distribution system but is a distinct disadvantage in gas line, because it is so difficult to purge. Then too, gaseous contaminators can be purged from a line relatively quickly, but it takes heat as well as a purging flow to remove water vapor completely from a line.

## Source of Inert-Gas Supply for Piping Systems

### *Description and Use*

Individual argon and helium cylinders can be connected directly to individual chambers or furnaces by means of tubing. A pressure regulator is attached to the cylinder to reduce the exiting gas pressures to the desired level; a rotameter is used to measure flow rates.

When warranted by consumption rates, two or more cylinders can be manifolded together in banks to provide a longer lasting source of supply and to reduce the amount of cylinder handling. A typical cylinder manifold setup is shown in Fig. 6. As may be seen, the manifold has two independent sets of controls to permit alternate or simultaneous operation of the two cylinder banks. Empty cylinders can be replaced with full ones after shutting down one or both cylinder banks. Somewhat similar methods are applicable to liquid argon supplied in cylinders.

For larger consumption rates ranging from 10,000 to 20,000 or more cu ft per month, bulk supply sources result in considerable savings in space and expense. One such source, illustrated in Fig. 7, consists of permanent receivers into which the gas is pumped from trailer trucks for use as required.

Another system for argon consumption rates ranging from 100,000 to 1,000,000 or more cu ft per month is shown in Fig. 8. It consists of a liquid-argon stand tank which is filled from trailer trucks or railroad tank cars. The liquid argon is converted into gas and piped as required to points of use throughout the plant.

### *Manifold Purging*

Special provisions should be made to allow the purging of cylinder manifolds when a cylinder bank has been

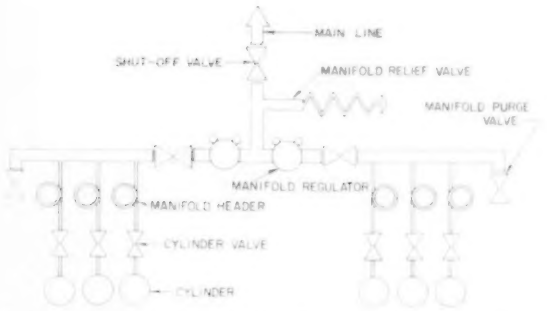



Fig. 9 Schematic sketch of cylinder manifold with purge valves on end

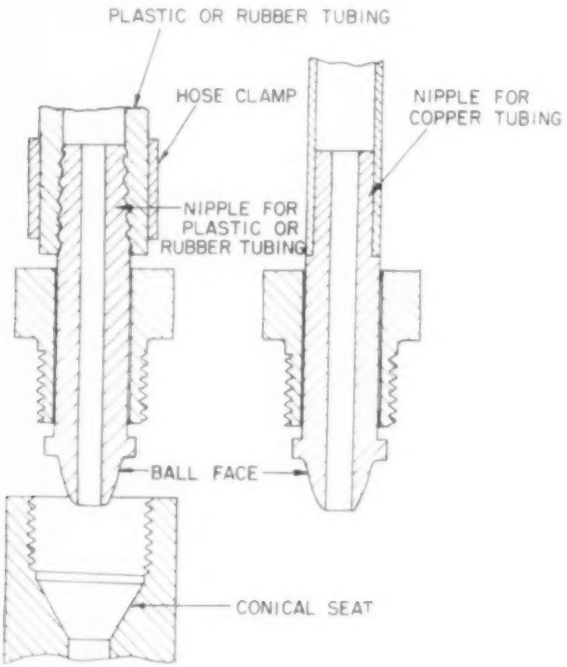



Fig. 10 Detachable connection for secondary hose from station outlet

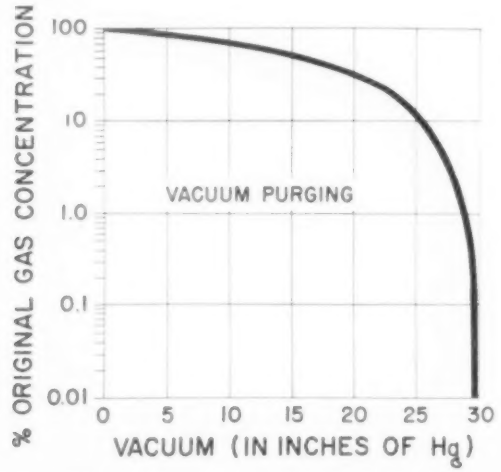



Fig. 11 Effect of vacuum purging on residual air in a chamber or furnace

shut down for recharging. As indicated in Fig. 9, a purging valve installed at the outside ends of each manifold bank will enable the line to be purged before bringing the bank back into service. It should be pointed out, however, that when empty cylinders are disconnected from the line, they should be quickly replaced with full ones to avoid a buildup of a moisture film on the interior surfaces of the manifold. Just before connecting a full cylinder to the manifold, the cylinder valve should be opened slightly to allow a blast of inert gas to clear the valve of gaseous and particle contaminators. (Permanent bulk supply sources are usually purged by the gas supplier.)

## Piping System Materials

### *Main and Branch Lines*

All main and branch-line piping should consist of seamless copper tubing. Fittings for connecting copper tubing should be of wrought or cast copper made especially for silver brazing the joints. A water-mixed silver-brazing flux with good fluxing action should be used; the flux should be such that it is easily removed from the assembly after brazing.

### *Valves and Station Outlet Equipment*

Valves installed in main branch lines should have either a brass or bronze body and should be selected for their ability to provide tight shutoff and freedom from external leakage. Packless diaphragm- or bellows-type valves are particularly recommended. All service outlets or station valves should have an outlet plug assembly. Each station outlet should be properly identified by means of a suitable sign which cautions against unauthorized use and directs that the outlet plug be attached to the valve outlet when the station is not in use.

### *Secondary Lines from Station Outlets*

The requirements for secondary lines such as flexible hose used at station outlets to supply individual process equipment are sometimes as exacting as in the case of main distribution lines. While standard inert-gas hose made of vinyl plastics is satisfactory for noncritical applications, it is unsatisfactory for critical applications requiring the ultimate in inert-gas purity. Instead, teflon or fluorothene plastics or small-diameter annealed copper tubing are recommended, depending upon the degree of flexibility required. It would be well to install a valve at a point close to the line end so that the line can be maintained at a positive pressure during shutdown periods. Following shutdown, the line can then be used immediately for purging; the need for long periods of purging prior to resuming process operation can thereby be avoided.

Detachable connections for plastic hose or copper tubing should provide durable gas-tight seals. Threaded couplings with ball-face and conical-seat joints are particularly recommended as shown in Fig. 10. This illustration also shows a new arrangement of threaded nuts corresponding to the International Acetylene Association's hose and tubing connections for

inert gas. Starting in January 1958, by industry-wide agreement, all new apparatus and gas line fittings must correspond to this standard.

Copper tubing, valves and fittings should be cleaned before installation by the methods outlined below. All connections should be checked with a nonfrothing leak-test solution with the gas line under a pressure of at least 50 psi. For maximum safety, a 24-hr pressure test is recommended. The line should be purged with upwards of 10 volumes of inert gas before starting process operation.

## Fabrication of Piping Systems

### *Cleaning*

Before erection, all metal tubing, valves and fittings should be thoroughly cleansed of oil, grease, dirt or other contaminating materials by washing in a hot aqueous solution of chemical cleaner such as trisodium phosphate. Scrubbing should be employed where necessary to ensure complete cleansing. After washing, the materials should be rinsed thoroughly with clean water.

### *Joints and Connections*

All joints in the tubing, except those in valves or equipment requiring screwed connections, should be made with soldered-type copper fittings. Suitable adaptors should be used for installation of valves and other equipment provided with threaded connections. In applying flux to a brazed joint, particular care should be exercised to avoid leaving any excess inside the completed joint.

Screwed joints made for attaching valves or other permanently connected equipment should be installed by tinning the male thread with soft solder before assembly. The tinning must provide thorough coverage of the threads, and the joint must be taken up a sufficient number of turns to provide normal thread engagement. Where heating is required to effect the proper assembly, adequate precautions must be taken to avoid damage to valves or equipment by overheating.

All bends and changes in direction requiring turns or offsets of radius less than five times the outside tube diameter should be made by soldered-type fittings or by tubing shaped with bending tools. All bends must be free from any appreciable flattening, buckling or thinning the tube wall of the joint. Constrictions that will allow gas to become pocketed and impede purging should be avoided. All branch connections or reducing joint ends should be solder-type fittings. All tube ends should be provided with valves to permit proper purging of the line and to allow for later extensions of the distribution system without shutting down.

### *Testing*

After erection of the piping but before installation of the outlet valves, the line should be blown clear with a dry gas. After the outlet valves are installed, each section should be subjected to a test pressure of at least 1½ times the maximum working pressure but not less than 50 lb per sq in. with a dry gas. This test pressure should be maintained until each joint has been examined for leakage by means of a nonfrothing leak-test solution. After completing the joint test, a final check for leakage should be made by allowing the pressure to remain in the piping for at least 24 hr. This test should include measurement of the temperature of the piping at the beginning and end of the test for accurately evaluating the relation of final and initial pressures as a measure of the leak tightness of the piping.

### *Drying*

All traces of moisture contained in the piping should be removed before the system is placed in service. This may be done by heating piping to a temperature of at least 150° F to vaporize any water it may contain. At the same time, dry gas should be passed through the piping to remove the water vapor. If a torch is used to supply the heat, it should traverse the entire length of the supply run to station outlets progressively in the direction of gas flow. After drying, all outlets or other openings in the piping system must be kept closed to prevent the re-entrance of moisture.

### *Final Purging Procedure*

The entire distribution piping system—including the manifolding at the source of inert-gas supply—must be purged before using the system to supply argon or helium for controlled-atmosphere processes. This is done by allowing argon or helium to flow through the system at a pressure of 5 to 10 psi. To minimize gas pocketing in the branch lines or station drops, each station valve should be opened to allow the purging gas to discharge continuously at a rate of 2 cu ft per hr until at least ten pipe-line volumes are used.

## Establishing Process Atmospheres

With a supply of high-purity inert gas on hand, the next step is to displace air from a process container and replace it with argon or helium. There is a wide variety of ways in which to accomplish this. The methods, however, can be divided into two basic categories—vacuum purging and inert-gas purging.

### *Vacuum Purging*

In vacuum purging, the chamber or process container is evacuated to some low absolute pressure and then back-filled with argon or helium. Starting with a 100% air atmosphere in the container at zero vacuum, residual air is proportionately reduced with increasing vacuum. The limit of zero percent air is reached when a perfect vacuum of 29.92 in. of mercury* (32° F) is produced in the chamber. The relationship between residual air and vacuum may be seen in Fig. 11 which shows a curve plotted on semilogarithmic coordinates. As can be seen, residual air approaches a level of 0.01% only when a vacuum near the ultimate is reached. Furthermore, slight variations in the vacuum near this limit cause relatively wide changes in the residual atmosphere. This is particularly noticeable when residual contamination must be less than 0.01%.

Theoretically, a vacuum of 77 microns of mercury is

* Equivalent to 760,000 microns of mercury.

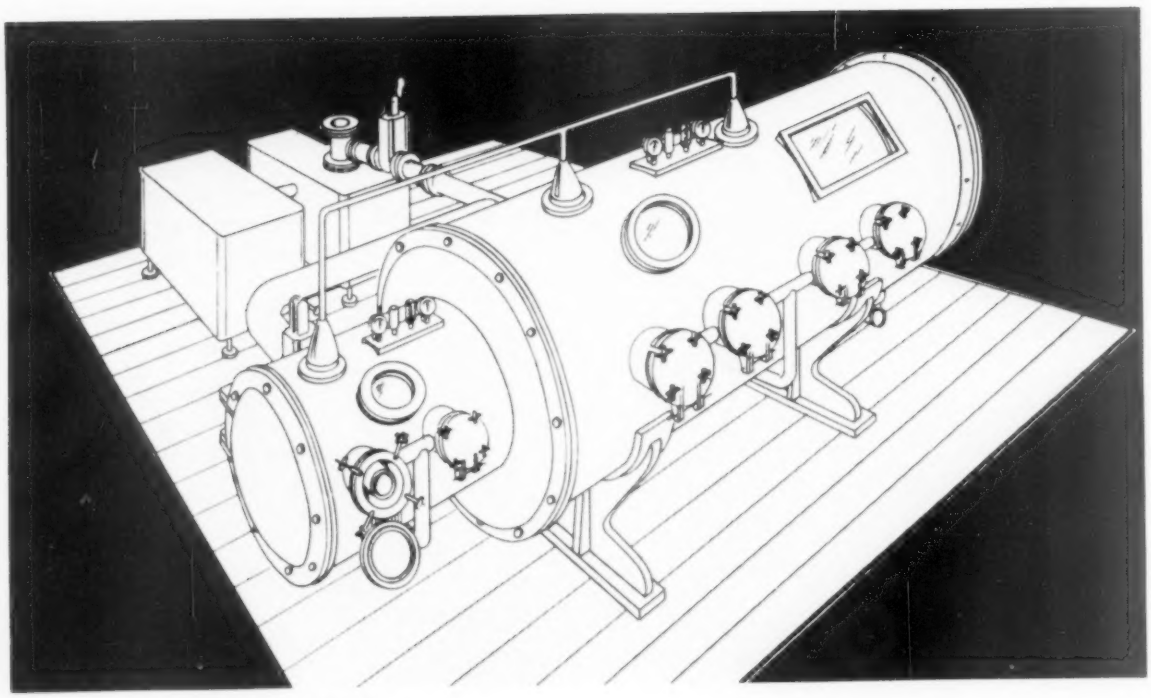


Fig. 12 Vacuum-purge welding chamber

required to reduce the air to a level of 0.01% or 100 parts per million. In actual practice, however, contamination by outgassing is encountered in micron pressure ranges. To outgas the chamber completely before back filling with inert gas, vacuums on the order of 0.1 to 0.01 microns of mercury are often used. For such high vacuums, a diffusion pump and possibly a cold trap are required in addition to a mechanical roughing pump. With the aid of heat to speed outgassing, the time required to evacuate a chamber properly may vary from approximately 30 min to 12 hr or more, depending on such factors as the size of the chamber, the capacity of the pumping system, the outgassing characteristics of the chamber and its component parts, and leak tightness of the entire system. A vacuum-purge system, however, when designed, installed and operated properly, can provide the ultimate in atmosphere purity.

Figure 12 shows a vacuum purge chamber used for welding applications. Two horizontal cylinders are joined to form a main welding chamber and an air lock. The vacuum pumping system is toward the rear with the vacuum piping connected to both the chamber and the air lock.

### *Inert-Gas Purging*

Inert-gas purging is accomplished by passing argon or helium into the process container and by venting off the air or air-inert gas mixture. A theoretical analysis of the purging methods can be made by considering air removal by perfect displacement, perfect mixing or a combination of the two methods. A mathematical model of perfect displacement can be represented in the ideal case by the action of a piston forcing air out of a cylinder. Assuming no clearance between the piston and cylinder end, the air would be completely removed at the end of one piston stroke. A flow of argon or helium started on the return stroke would provide the inert atmosphere. If no air leakage into the cylinder is allowed, then an inert atmosphere of maximum purity is established with one volume of inert gas.

For perfect mixing, we can consider, as an idealized mathematical model, the complete and instantaneous dilution of air in the chamber by means of a flow of inert gas. For an inert-gas volume equal to $\frac{1}{10}$th the chamber volume, air in the chamber would be diluted equal to a ratio of $\frac{9}{10}$ or approximately 91%. The excess air-inert gas mixture would then be vented off. The next sequence would result in an air-inert gas mixture in the chamber amounting to 83% air.

If we consider the operation as a smooth continuous flow, the formula for residual air concentration during any part of the purging cycle becomes $C = e^{-N}$ where C equals the air concentration and N equals the number of chamber volumes of purge gas. Here, again, we assume a leak-free system. Figure 13 shows a semilogarithmic graph of the two curves: one for perfect displacement and one for perfect mixing. The curves are plotted in terms of residual air vs. the number of chamber volumes of inert gas used to purge the chamber. From the graph, it can be seen that the residual air is reduced to about 0.01% or 100 parts per million by a single-volume purge using the perfect displacement technique or by a nine-volume purge with the perfect

mixing technique. In practice, however, neither perfect displacement nor perfect mixing can be achieved. One is accompanied by the other with the result that the number of volume purges used to reduce air to a concentration of 0.01% has ranged from a low of about six or seven volumes up to an infinite maximum. A number of purging factors is involved: leak tightness of the system, shape of the chamber and internal parts, location and configuration of inlet and outlet ports and gas-flow rates.

A laboratory investigation of purging practices is now under way wherein the aforementioned purging factors are being subjected to careful attention. The object of this work is to approach the ideal case of perfect displacement more closely in an effort to obtain maximum atmosphere purities with a minimum number of gas volume purges.

A major advantage to be gained by using inert-gas purged chambers arises from the fact that such chambers are not pressure vessels. They can, therefore, be designed for low cost and maximum utility. Furthermore, purging with either helium or argon can be carried on in such chambers simultaneously with process operations such as welding or heat treating so as to prevent localized buildup of contaminators.

### *Combination Techniques*

In an effort to reduce equipment and purging costs, combination techniques have been used in which a vacuum is produced with standard mechanical vacuum-pump equipment and followed by back-filling with inert gas. The cycle is repeated several times. In another approach, collapsible chambers have been used for welding applications. The rubber or plastic walls of these chambers are repeatedly collapsed, and the chamber is back-filled with inert gas to its full volume.

Either approach can be effective for producing fairly good inert atmospheres in the chamber, especially when cold traps are used to remove condensible vapors.

## Summary

Argon and helium are available at high levels of purity—in excess of 99.995%. Furthermore, it is now possible to obtain argon in which the purity of the delivered product is guaranteed to be not less than 99.995% equivalent to a combined total of all impurities amounting to not more than 50 parts per million. Consequently, the purity of these gases as they come from the supplier presents no problem for controlled-atmosphere processes. Helium is presently in short supply, but adequate quantities usually can be obtained whenever it offers some definite technical advantage. Argon is available in any desired quantity.

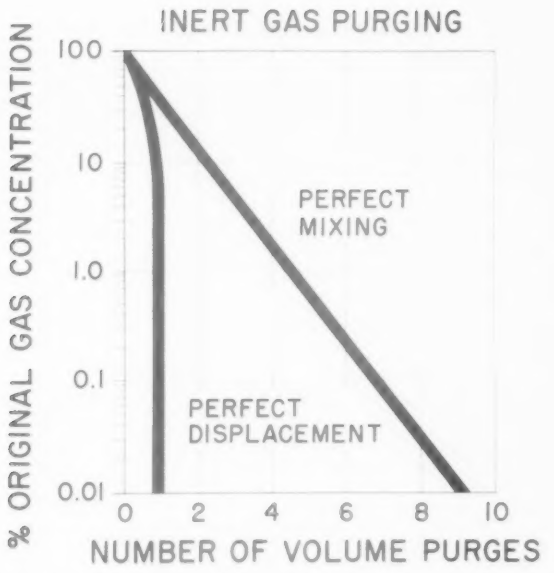



Fig. 13 Effect of inert-gas purging on residual air in a chamber or furnace

Difficulties with contaminator pickup may arise when otherwise high-purity argon and helium are used to purge chambers and furnaces. These difficulties may, however, be avoided in two ways: first, by the installation of distribution piping systems specifically designed to preserve the original purity of the inert gas and, second, by properly cleaning and purging these systems before their use. Finally, the effective application of argon or helium for controlled-atmosphere processes will depend upon the utilization of appropriate purging techniques for chambers or furnaces.

# SUBMERGED-ARC WELDING OF URANIUM

*Major developments make possible the successful application of the submerged-arc welding process to the fabrication of uranium weldments*

BY GALE S. HANKS, J. M. TAUB AND E. L. BRUNDIGE

ABSTRACT. The submerged-arc welding process has been developed for making satisfactory weldments of uranium metal. It is not practical to weld uranium without a protective atmosphere because of the metal's tendency to oxidize rapidly, and because of its pyrophoric nature. The submerged-arc process offers a welding process where the flux forms a fluid protective blanket above the liquid filler metal. Both fused flux and filler metal cool and solidify under the unfused flux.

None of the commercial fluxes for use with ferrous and nonferrous metals proved suitable for use in the submerged-arc welding of uranium.

It was necessary to develop a new flux in order to use the submerged-arc process for making uranium weldments. This flux, as finally developed, consisted of 75 wt. — % of the mineral fluorspar and 25 wt-% of the mineral cryolite. The mixture was made as a frit with a controlled particle size.

Properties of weld metal made by submerged-arc welding using the above developed flux showed tensile strengths nearly 65% greater and yield strengths approximately 95% greater than those of as-cast metal.

## Introduction

The welding of uranium by various methods has been under investigation at the Los Alamos Scientific Laboratory for a number of years. The results of this early work were reported in a declassified report LA-1449.[1] Recent developments have been reported at various technical society meetings.[2,3] Most of the work covered in these papers was concerned with the fusion welding of uranium using the inert-gas-shielded arc-welding process. However, a short discussion was given of the inert-gas-shielded metal-arc method or shielded-arc consumable-electrode method.

For purposes of joining heavy sections of metal, such as thick plate, the consumable-electrode process appears to be the most suitable. This process has its drawbacks when used for joining uranium, especially with uncoated filler rod.

## Submerged-Arc Process

Where the joining of heavy sections of uranium metal had particular interest, it was thought that the submerged-arc process might offer a suitable means to accomplish this type of welding. Submerged-arc welding as practiced commercially on steel is characterized by the use of high welding currents. The current carried, in relation to the cross-sectional area of the electrode, is often six times as great as the current carried in manual shielded-arc welding.

Very deep penetration can be obtained with a simple butt joint, or with only a small groove.

In respect to uranium, its tendency to oxidize rapidly, its pyrophoric nature and its poor electrical conductivity make a welding process attractive where the effects of these factors are reduced. It is nearly impossible to weld uranium without a protective inert-gas atmosphere to cover the molten metal during the welding process.

In the submerged-arc welding process, the molten flux forms a fluid protective blanket above the liquid filler metal as welding progresses. Both fused flux and filler metal cool and solidify under the unfused flux.

## Other Welding Processes Tried on Uranium

Heavy sections of uranium have been welded successfully by the use of filler rod and using a shielded-arc torch. However, the part to be welded generally becomes somewhat oxidized unless the weldment is completely surrounded by an inert-gas envelope. Plating the uranium welding rod with silver protects it, and to some extent the welded part, against oxidation, but does introduce a contaminator in the weld metal. This is not a serious offense as silver is not soluble in uranium and will separate to form a cover on the weld metal. However, there is always the possibility that silver can be trapped in the bead when making additional passes over the first weld bead.

It was thought that the submerged-arc process would escape the disadvantages of the shielded-arc method and give a method of welding uranium under a protective medium without the use of such shielding equipment as dry boxes, etc. By submerging the molten weld metal beneath a protecting layer of liquid flux, during fusion with the base metal, a bare rod could be used for welding. After fusion had taken place, the flux would remain as a surface layer protecting the hot weld region from contact with air and consequent oxidation.

## Equipment Used

Figure 1 shows the typical setup used for the development work on the submerged-arc welding of uranium.

The power source was a motor-generator supplying

**Gale S. Hanks, J. M. Taub** and **E. L. Brundige** are associated with the University of California, Los Alamos Scientific Laboratory, Los Alamos, N. Mex.

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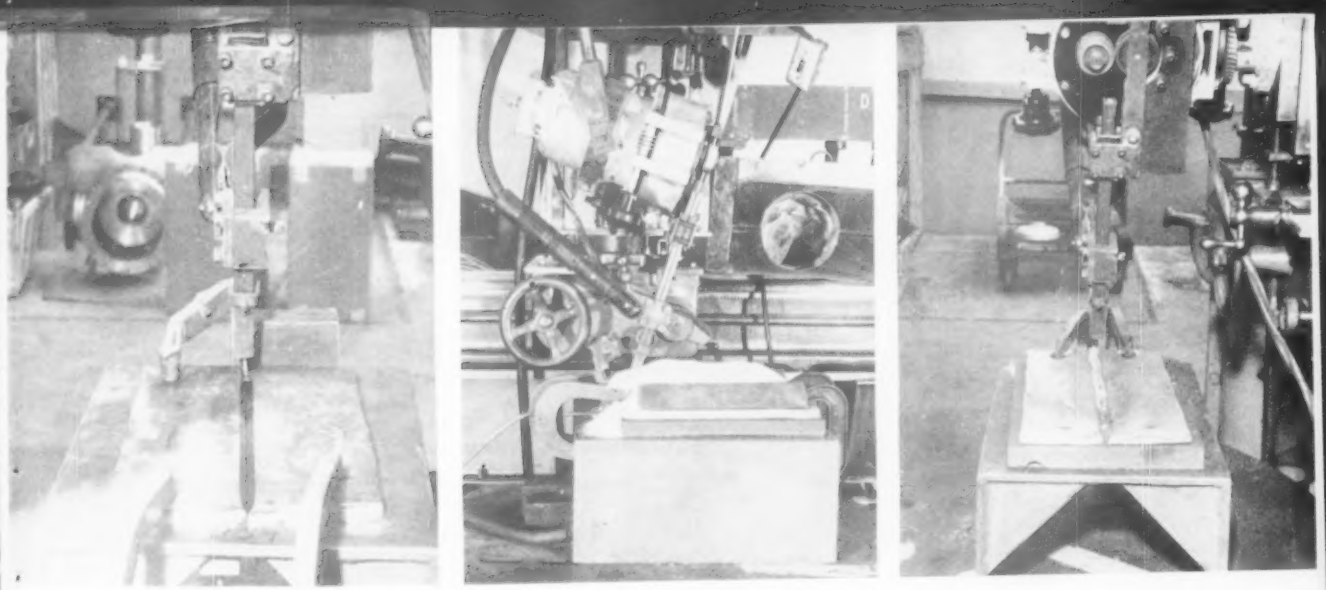


Fig. 1 Setup for submerged-arc welding of uranium (75% fluorspar – 25% cryolite)

d-c current to an automatic welding head mounted on a movable carriage. The arc was started by the use of a high-voltage, high-frequency current in conjunction with a piece of steel wool interposed between the electrode and the work.

## Commercial Fluxes Tried

An important part of submerged-arc welding concerns the flux used. In the commercial submerged-arc welding of ferrous and nonferrous metals, fluxes usually consist of granulated fusible mineral materials substantially free from substances that are capable of evolving large amounts of gases during welding. Although silica is an ingredient, there is no free silica in the finished weld metal.

These fluxes are made to many different specifications and several ranges of particle size. The choice of flux, of course, depends upon the welding procedure to be employed, the type of joint and the composition of the material to be welded.

There is a variety of fluxes available on the market and it was thought that at least one flux would probably show some promise for use with uranium.

A number of the commercial fluxes made for submerged-arc welding of ferrous and nonferrous metals was investigated. None of the fluxes tried showed much promise of satisfactory operation for use in the welding of uranium. Variation of particle size as well as different mixtures and combinations of commercially available fluxes were tried without appreciable success.

Figure 2 shows several welds made using commercial fluxes. None of these commercial fluxes showed promise due to the reactive nature of uranium. All of the weld metal contained trapped slag and the fused flux above the weld bead sometimes contained as much as 10% by volume of trapped uranium due to splatter from welding. The arc was also very unstable.

The experience with commercial fluxes indicated that a new approach was in order and that for the successful

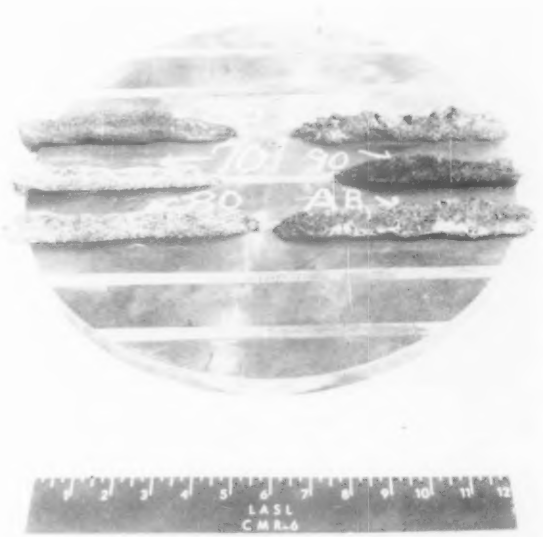



Fig. 2 Submerged-arc-welded uranium (Commercial fluxes)

adaptation of the submerged-arc welding process for use on uranium metal, a new welding flux must be developed.

## Development of Flux

A considerable number of materials was tested in searching for a suitable flux. In this search, both the minerals fluorspar and cryolite were tried and indicated some promise. The fluxing action of these two minerals was somewhat different in that the cryolite produced a shallow wide weld while the fluorspar gave a narrow penetrating weld. It seemed logical, therefore, to try a mixture of these two minerals. A search of the literature indicated that the eutectic mixture would be about 80 wt-% fluorspar and 20 wt-% cryolite.

A number of welds was made using this composition,

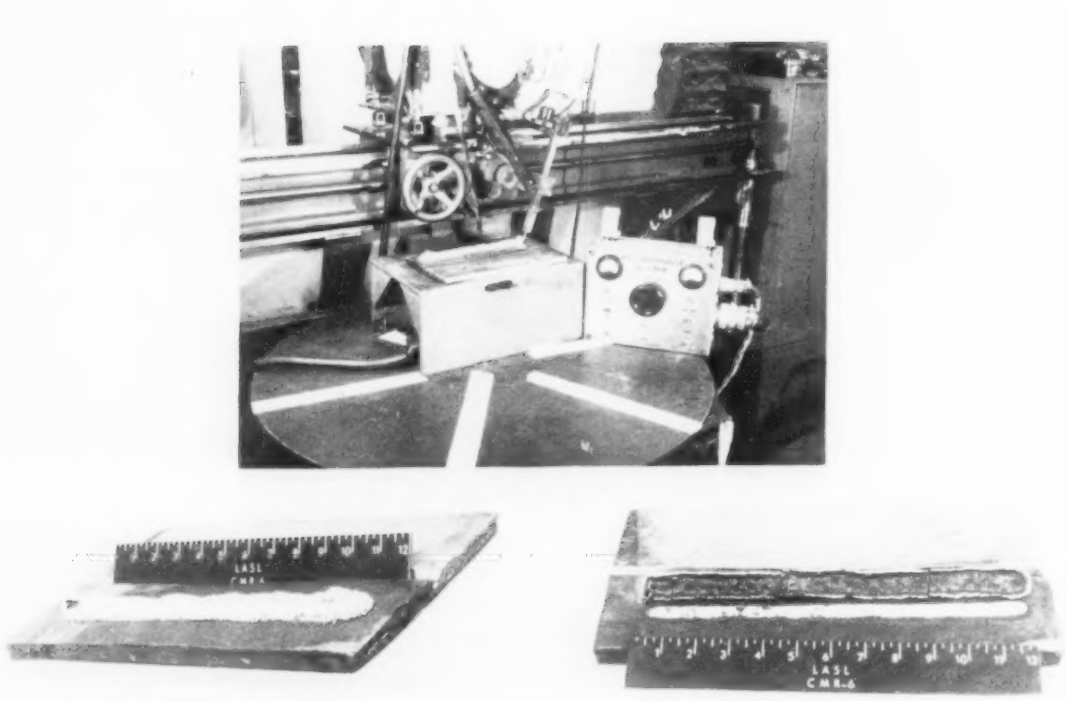



Fig. 3 Setup for submerged-arc welding of uranium. (Fluorspar 75% – cryolite 25%)

and a large amount of valuable data was obtained. The composition was later changed to 75% fluorspar and 25% cryolite as this ratio was found to approach more closely the eutectic composition.

In the early development work it was soon found that, although the combination of fluorspar and cryolite seemed to work fairly well, it was not completely satisfactory. The weld metal deposited was dirty, unsound and porous, and was also full of flux inclusions and gas pockets. From these observations, it was indicated that the purity of the flux was very important.

Technical grade or chemically pure cryolite and fluorspar were obtained. These materials were too fine, as far as mesh size was concerned, for use as a welding flux. The development of a high-purity flux possessing a suitable particle size for use in the submerged-arc welding of uranium proved quite a lengthy development program in itself.

A "fritting" procedure was borrowed from the ceramics industry. In this method the high-purity fluorspar and cryolite were mixed in the desired proportions, melted, held at temperature until all volatile impurities had burned out and then poured into water. The resultant frit was dried, screened and used as a flux.

The use of this prepared flux proved quite successful. Sound welds were made; however, there were still some slag inclusions and the fused flux failed to break away cleanly from the weld metal.

This problem was finally solved by using a controlled particle size of flux. The flux size as finally developed was −14 +28 mesh for heavy plates $\frac{1}{4}$ in. and over in thickness and −28 +60 mesh for thicknesses $\frac{1}{4}$ in. or less.

## Typical Setup for Submerged-Arc Welding of Uranium

Figure 1 shows the typical setup for the submerged-arc welding of uranium. The filler rod used was uranium rod $\frac{3}{16}$ in. in diameter having approximately one to two mils of electro-deposited silver on its surface. A single 60-deg V was used as the joint design and the plates shown being welded together are approximately $\frac{3}{4}$ in. in thickness. A graphite backing plate containing a small groove, helium flushed, was used under the uranium plates being welded. The surface of the uranium plates was electropolished prior to submerged-arc welding.

The plate shown in Fig. 1 was welded using d-c straight polarity of approximately 500 amp and 28 to 30 v. The speed of travel was approximately 140 ipm with the electrode angle between 17 and 18 deg lagging.

Figure 3 again shows a typical setup used for the submerged-arc welding of uranium. The top photo shows the plate immediately after welding with the excess flux removed. This plate can be seen at close range on the photo in the lower left and the photo at the lower right shows the weld bead with the flux broken off.

## Flux Removed Easily

As can be seen from the photograph in Fig. 3, the flux breaks off cleanly from the weld metal leaving a

uranium weld bead that is clean and sound. The white coloring on top of the weld bead is metallic silver from the uranium electrode coating.

## Preparation of Welding Rod

The silver deposit on top of the weld metal allows another pass to be made without fear of the uranium oxidizing between passes.

The use of a bare uranium rod as the consumable electrode resulted in spattering with entrapment of metal in the slag and the retention of slag particles in the weld metal. Spattering was minimized when the uranium welding rod was coated with one or two mils of electrodeposited silver. The arc was much more stable when using silver-plated rod. Contamination of the weld metal by uranium oxide, from the rod being heated by the welding arc where the rod was unprotected by the flux cover, was almost reduced to zero when using silver-plated rod. It is probable that the silver improved current conduction and smoothed the transfer of metal from the rod to the weld region.

## Microstructure of Welds

Figure 4 shows the typical microstructures of the weld metal and of the base metal of submerged-arc-welded uranium. In this particular weld sample, the base metal was an "as-cast" uranium plate which had been hot rolled approximately 30%, with the result that the grain size of the base metal was somewhat smaller than the grain size of cast uranium.

The large carbides of the base metal are plainly visible, while in the weld metal the carbides have been broken up, perhaps by the arc energy, to a very fine size. It was thought for some time that the fine particles appearing in the weld metal were either silver from the electrode coating or trapped uranium-oxide particles.

Figure 5 shows the microstructure of uranium weld metal deposited by using a tungsten-arc torch and either silver-coated uranium rod or bare uranium rod as the filler material. The weld metal of a submerged-arc weld is also indicated for a-c, d-c straight polarity and d-c reverse polarity.

The indications are that uranium deposited by any form of welding is characterized by the fine particle size of the various inclusions.

The evidence suggests that most of these small particles are broken up carbides. Some proof for this observation is offered by the microstructures shown in Fig. 4 of the weld metal and the base metal. There are large carbides in the base metal and, presumably, small carbides in the weld metal.

In general, the weld bead shows the characteristic cast structure of fused metal. This structure changes

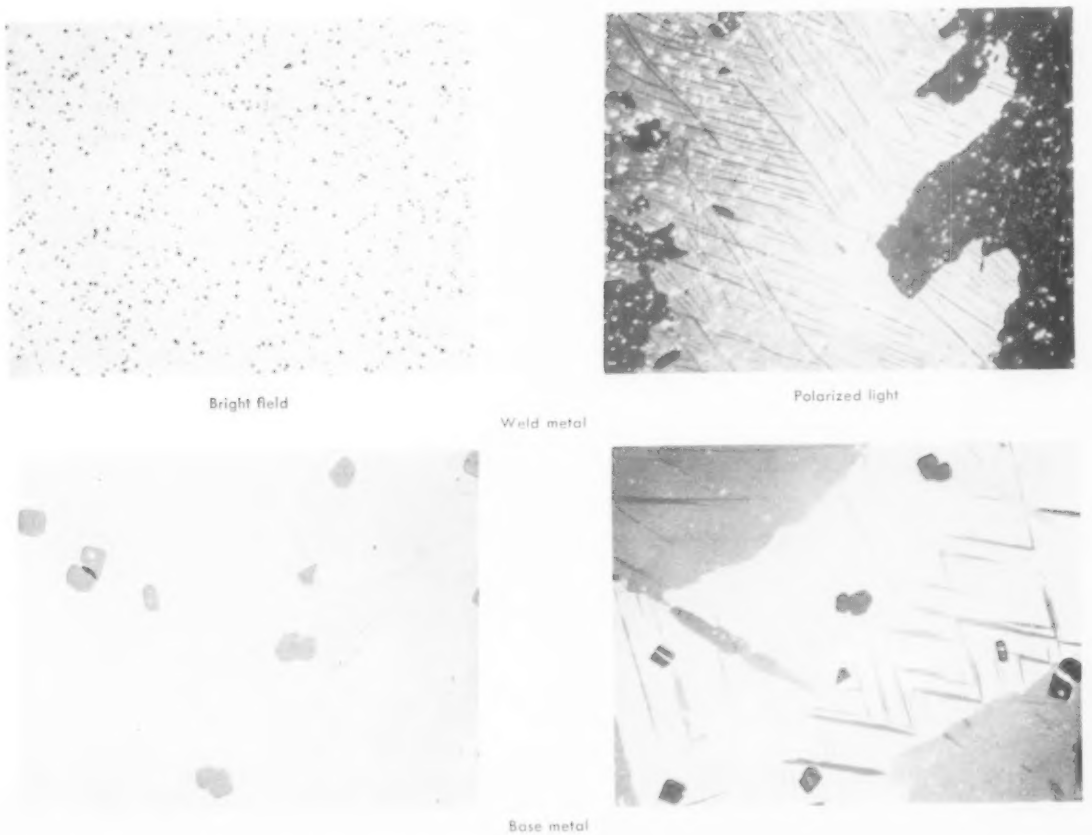



Fig. 4 Microstructure of submerged-arc-welded uranium. X 250

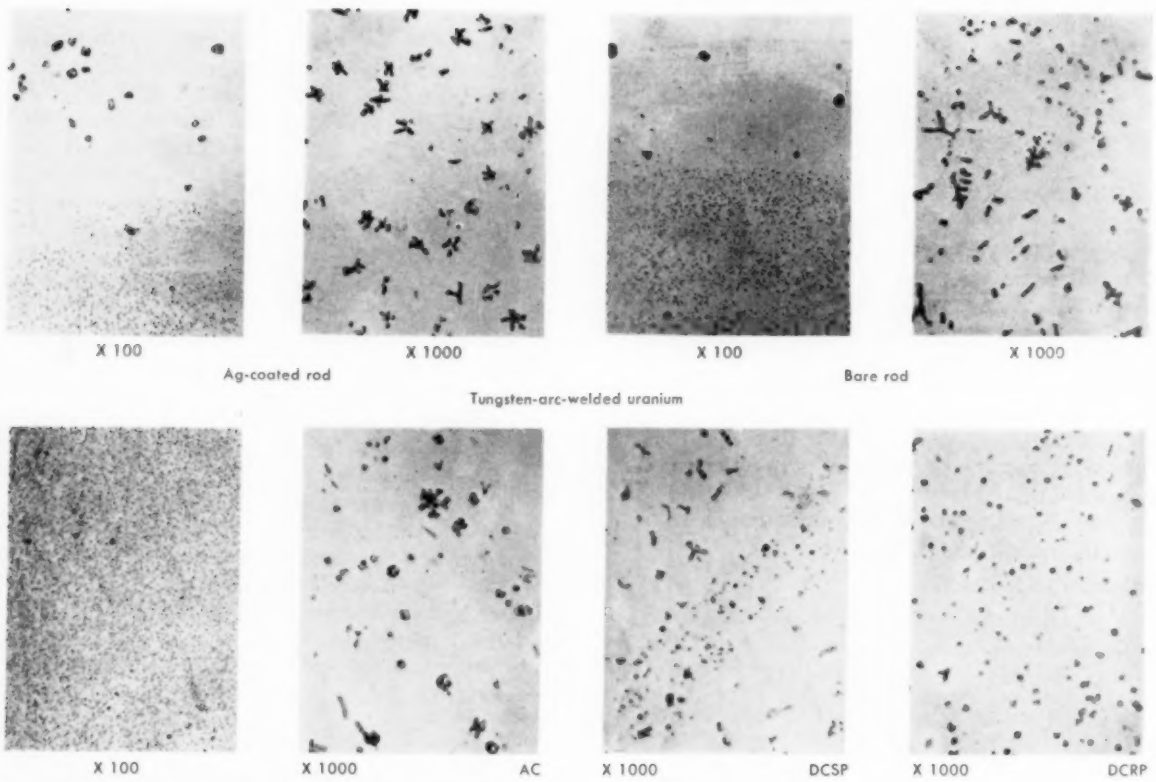



Fig. 5 Submerged-arc-welded uranium; 500 amp (80% fluorspar – 20% cryolite)

to that of uranium heated into the gamma, beta and alpha regions as one progresses from the weld into the base metal.

## Physical Properties of Welds

Table 1 presents some of the mechanical properties of submerged-arc welded uranium. As stated previously, the base metal used for this investigation was cast uranium plate which had been hot rolled approximately 30 %. The base metal, therefore, was some 45% stronger in tensile strength than as-cast uranium.

Table 1 presents the impact and shear strength of the weld metal and base metal of submerged-arc-welded uranium plates. The properties of "as-cast" metal are also shown for comparison.

Table 1—Physical Properties of Submerged-Arc-Welded Uranium

| *Impact Strength* | | | | | *Shear Strength* | | | | |
|---|---|---|---|---|---|---|---|---|---|
| | *Test* | *—Charpy impact values, ft-lb—* | | | | *Sample* | | | |
| *Sample no.* | *temperature, ° C* | *Weld metal* | *Base metal* | *Cast metal* | *Sample no.* | *diam, in.* | *Weld metal* | *Base metal* | *Cast metal* |
| 1 | −40 | 5.5 | 5.5 | | 1 | 3/8 | 80,600 | 67,000 | |
| | | | | | 2 | 3/8 | 79,700 | 62,800 | |
| 2 | −40 | 6.0 | 5.0 | | 3 | 3/8 | 78,500 | 64,900 | |
| | | | | | 4 | 3/8 | 82,100 | 69,500 | |
| 3 | −40 | 5.5 | 6.0 | | 5 | 3/8 | | 67,400 | |
| Avg. | −40 | 5.7 | 5.5 | 16.0 | Avg. | 3/8 | 80,200 | 66,300 | 75,000 |
| 1 | +28 | 8.0 | 7.5 | | 1 | 1/4 | 82,500 | 67,200 | |
| | | | | | 2 | 1/4 | 84,700 | 69,200 | |
| 2 | +28 | 7.0 | 8.0 | | 3 | 1/4 | 84,700 | 66,600 | |
| | | | | | 4 | 1/4 | 81,500 | 68,000 | |
| 3 | +28 | 8.0 | 9.5 | | 5 | 1/4 | 81,700 | 68,300 | |
| Avg. | +28 | 8.7 | 8.3 | 24.0 | Avg. | 1/4 | 83,000 | 67,900 | 84,000 |
| 1 | +92 | | 9.0 | | 1 | 1/8 | 89,000 | 73,600 | |
| | | | | | 2 | 1/8 | 85,600 | 72,800 | |
| 2 | +92 | | 8.0 | | 3 | 1/8 | 90,300 | 72,400 | |
| | | | | | 4 | 1/8 | 89,600 | 73,400 | |
| 3 | +92 | | 10.0 | | 5 | 1/8 | 93,700 | 75,000 | |
| Avg. | +92 | | 9.0 | 32.0 | Avg. | 1/8 | 89,600 | 73,400 | 85,000 |
| Hardness DPH | | 260 | 220 | 200 | | | | | |

Table 2—Physical Properties of Submerged-Arc-Welded Uranium

Tensile and yield strength, $\frac{1}{2}$ elongation†

| Sample no. | Weld metal* Tensile strength, psi | Weld metal* Yield strength, psi | Weld metal* Percent elongation/in. | Base metal Tensile strength, psi | Base metal Yield strength, psi | Base metal Percent elongation/in. | Cast metal Tensile strength, psi | Cast metal Yield strength, psi | Cast metal Percent elongation/in. |
|---|---|---|---|---|---|---|---|---|---|
| 1 | 90,000 | 42,800 | 7 | 79,100 | 34,900 | 11 | 52,800 | 17,500 | 9.0 |
| 2 | 91,200 | 53,200 | .. | 75,200 | 28,800 | 8 | 45,300 | 20,700 | 5.5 |
| 3 | 87,000 | 36,300 | 6 | 75,200 | 29,000 | .. | 52,900 | 17,700 | 10.0 |
| 4 | 85,000 | 35,000 | 7 | 77,400 | 34,100 | 8 | 56,900 | 22,800 | 8.5 |
| 5 | 84,500 | 34,200 | .. | 77,200 | 32,200 | 9 | 56,200 | 26,100 | 5.0 |
| Avg. | 87,500 | 40,300 | 7 | 76,800 | 31,800 | 9 | 52,800 | 20,900 | 7.6 |

* All tensile samples broke in the base metal and not in the weld metal.
** Yield strength at 0.2% offset of a $29 \times 10^6$ modulus.

The impact strength of the weld metal and base metal is about the same at all testing temperatures.

It is interesting to note that the shear strength of the weld metal is equal to or greater than the shear strength of "as-cast" metal.

The weld metal had a hardness of approximately 260 DPH compared with about 200 DPH for "as-cast" material.

Table 2 gives the tensile and yield strengths of submerged-arc-welded uranium plates. As stated previously, the base metal was hot-rolled uranium plate which had approximately 45% greater tensile strength than "as-cast" uranium.

It is interesting to note that the weld metal was nearly 65% greater in tensile strength and about 95% greater in yield strength than "as-cast" metal.

These figures give ample insurance that one need not worry about the strength of a weldment made on cast uranium.

Figure 6 shows a weldment of uranium being fabricated by submerged-arc welding. For practical reasons, this particular casting was made in two pieces. The cast parts were joined by submerged-arc welding and the finished weldment machined to size.

## Summary

In summarizing, one can state that the process of submerged-arc welding can be applied satisfactorily to the fabrication of uranium weldments.

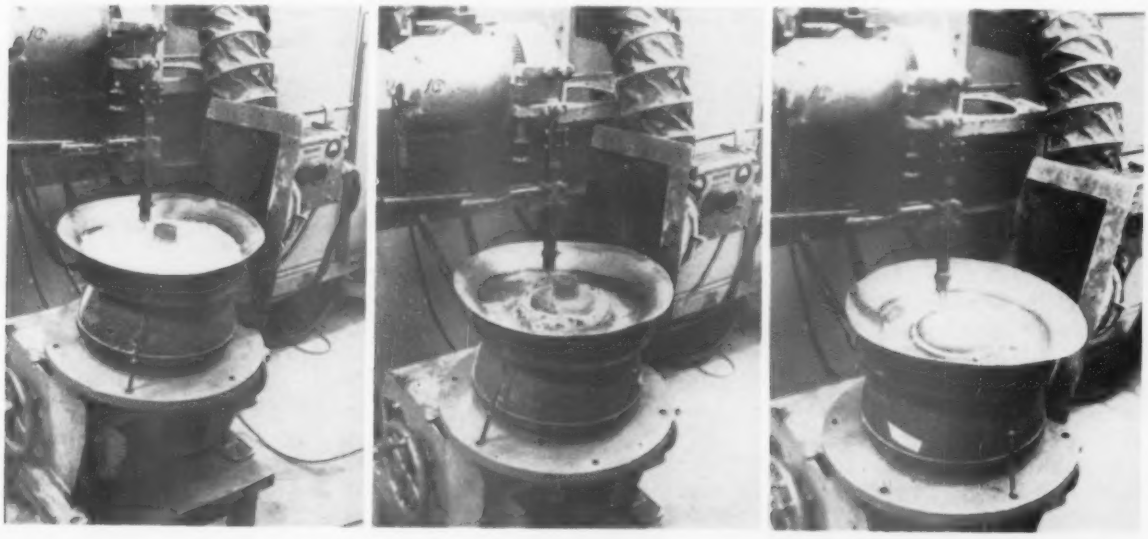
The major developments which have made this possible were:

1. The discovery of a suitable flux consisting of 75% fluorspar and 25% cryolite.
2. The development of a "fritting" process to improve the purity from raw materials to usable welding flux.
3. The selection of the correct particle size of the flux for use with uranium; −14 +28 mesh for heavy plates and −28 +60 mesh for thin plates.

## Future Work

Future development work will include a study of joint design, welding speed, penetration, electrode ma-

Fig. 6 Uranium weldment fabricated by submerged-arc welding



terial, backing, electrode tilt and other factors of interest which affect the success of the process.

*Acknowledgment*

The authors wish to acknowledge the valuable assistance of the following persons who were instrumental in helping to develop the submerged-arc welding process for making uranium weldments: J. D. Duncan, R. Montoya, T. Crisler and D. Fisher.

With the exception of Mr. Duncan, who is now employed as a metallurgist with Babcock and Wilcox, all personnel are employed by the Los Alamos Scientific Laboratory, Los Alamos, N. Mex.

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2. Brundige, E. L., *et al.*, "Fusion Welding of Uranium," *Nuclear Metallurgy*, **IV**, 107–115, The Metallurgical Society, AIME, New York (1957).
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## DISCUSSION BY HALLOCK C. CAMPBELL

This is a historical paper from the AWS viewpoint. The suggestion that so reactive a metal as uranium can be welded by submerged-arc technique is an entirely new and exciting thought. Even though most of the studies in this paper were completed five years ago, the work was classified until this very month, and thus the WELDING SOCIETY at large did not become aware of its existence until the program for this meeting was published in THE WELDING JOURNAL.

I think it is significant that the authors had to develop a new flux to accomplish their aim. Uranium is certainly an unusual metal. May we ask for some insight into its properties to help us understand the paper? What is its melting point compared to the steels we otherwise weld by submerged arc? What is the melting point of the fluorspar-cryolite flux? The commercial fluxes mentioned by the authors probably melted around 1800° F.

I judge that the inert-gas welds showed no particular arc instability, unless something more than oxidation trouble was involved in the "drawbacks. . .with uncoated filler wire." This suggests that the fluorspar-cryolited flux alters the emission characteristics of the wire. The starts, in fact, would seem to have been quite difficult. Most welding operators are adept at using the ball of steel wool; more elegant is the use of a high-frequency spark; but it is uncommon to say the least to have to use both. Was the HF starter any help if continued during the entire weld?

The silver protective coating seems to have stabilized the arc to a noticeable degree. I wonder whether an emissive coating of some cesium or rubidium compound, such as has been used by some other equipment manufacturer in high-speed inert-gas welding, may prove necessary in order to obtain further stability?

This is an intriguing new field of welding, with restricted application but of great interest to those who are on the outside looking in. Clues and guideposts for its development will have to be searched for everywhere. The casting operations may teach us much, particularly since only small melts dare be made, for fear of exceeding the radioactive critical mass. Perhaps the authors can comment on the normality or abnormality of the foundry practice, which on a larger scale is presumably similar in many ways to the "casting" operation of arc welding.

**Hallock C. Campbell** is Director of Research, Arcos Corp., Philadelphia, Pa.

## AUTHORS' CLOSURE

In commenting on Dr. Campbell's remarks, we certainly agree that uranium is an unusual metal. Its poor electrical conductivity makes it somewhat comparable to stainless steel as far as welding is concerned.

Its pyrophoric nature makes it also somewhat similar to magnesium from a welding standpoint.

The melting point of uranium is 1133° C, which is somewhat lower than most steels. The melting point of the fluorspar-cryolite flux used for submerged-arc welding is approximately 900° C.

It is quite difficult to start the weld cycle and, often, high-frequency current, as well as a ball of steel wool, is necessary for arc initiation. The high-frequency current is often used throughout the entire weld cycle.

The silver coating on the uranium welding rod may be said to serve a fourfold purpose as follows:

1. It protects the uranium from excessive oxidation above the arc zone.
2. It provides a coating on the uranium rod which has much better electrical conductivity than uranium.
3. It helps to stabilize the arc.
4. It forms a protective blanket over the weld deposit, preventing oxidation during multiple-pass welding.

Development work is under way using various coatings on the welding rod to obtain better arc stability.

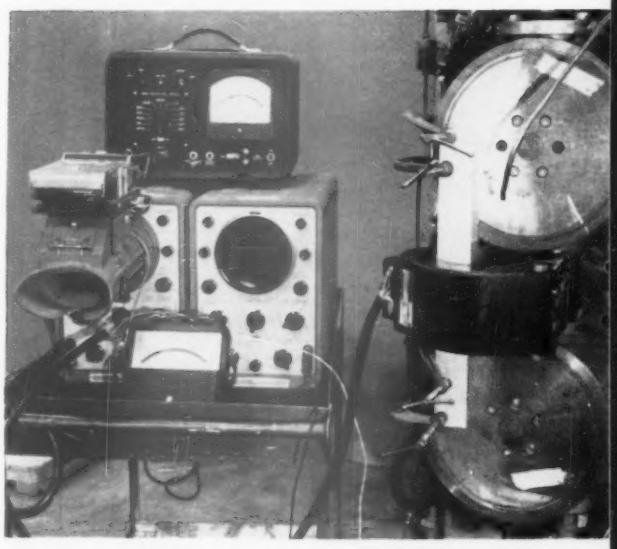

Fig. 1 Instruments for measuring welding current

# AN INVESTIGATION OF LAP SEAM WELDS IN 17-7 PH STAINLESS STEEL

*Development and calibration of instrumentation for measurement of welding current are described. Also, data on the seam welding of 17-7 PH stainless steel to be used in the TH-1050 condition are presented*

BY E. J. FUNK AND A. C. WILLIS

INTRODUCTION. The availability of new materials, both metallic and nonmetallic, has made possible the design of aircraft components of greater strength-weight ratio than has been possible heretofore. Prominent among these new materials are the high-strength steels, including the precipitation-hardening stainless steels.

One of the oldest of these steels available in sheet form is 17-7 PH, which was introduced ten years ago. The ability to be severely formed, plus good strength at elevated temperatures, has led to its use in many applications, both military and non-military.

All structural materials are limited to some extent in their usefulness by the joining methods available. This is particularly true in the case of high-strength high-temperature metals, and 17-7 PH is no exception. Any addition to the store of joining knowledge concerning this metal should be useful.

Meanwhile, the measurement of secondary current in a resistance seam welder with nonsinusoidal output has been a knotty problem. The recent development of new equipment has simplified such measurement.

## Purpose

The purpose of this paper is twofold: first, to describe a method for measuring the secondary current in a seam welder with direct energy 3-phase primary, and for calibrating the measuring equipment; second, to present data on the seam welding of 17-7 PH stainless-steel sheet to be used in the TH-1050 condition. These data will include the effect of certain welding variables, with a discussion of defects found, and the reduction of joint strength at elevated temperatures.

Table 1—Chemical Composition of 17-7 PH Stainless Steel

| | |
|---|---|
| Carbon | 0.09% maximum |
| Manganese | 1.00% maximum |
| Phosphorus | 0.04% maximum |
| Sulfur | 0.04% maximum |
| Silicon | 1.00% maximum |
| Chromium | 16.00–18.00% |
| Nickel | 6.50–7.75% |
| Aluminum | 0.75–1.50% |

## Metallurgical Considerations

Any discussion of the welding of 17-7 PH stainless-steel sheet necessitates some understanding of the metallurgical qualities of the material. The composition of 17-7 PH is given in Table 1, and is seen to be quite similar to that of a conventional low-carbon 17-7 stainless steel. Like the more common austenitic stainless steels, it is most often purchased in the annealed state, and can be severely formed in this condition.

The important difference lies in the aluminum content, which enables the steel to be hardened by precipitation. In addition, the aluminum, being a strong ferrite former, causes the microstructure to contain a noticeable amount of delta ferrite.

**E. J. Funk** and **A. C. Willis** are test engineers in the Experimental Engineering Materials Laboratory of Temco Aircraft Corp., Dallas, Tex.

Paper presented at AWS 1958 Annual Spring Meeting held in St. Louis, Mo. April 14–18.

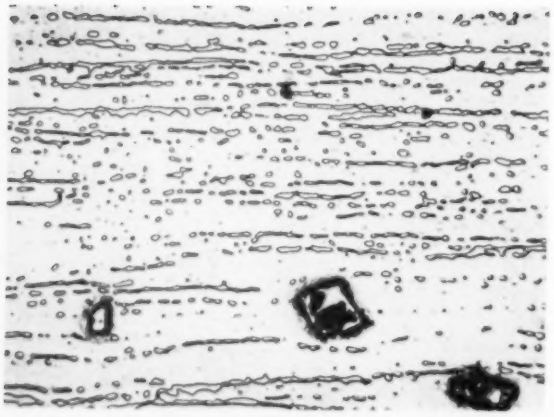

Fig. 2 Microstructure of 17–7 PH stainless-steel sheet, Condition A. Etched with Marble's reagent. X 500

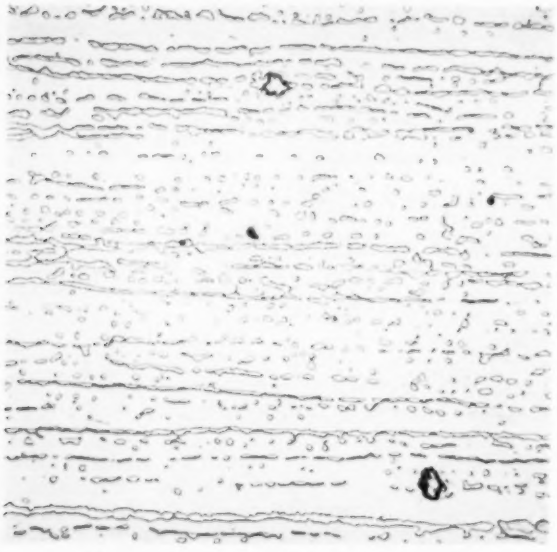

Fig. 3 Microstructure of 17–7 PH stainless-steel sheet, Condition T. Etched with Marble's reagent. X 500

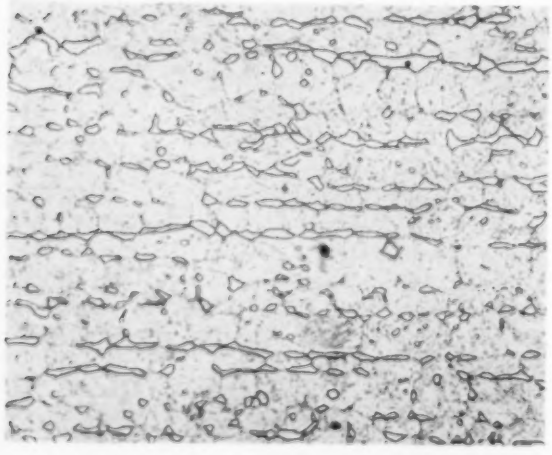

Fig. 4 Microstructure of 17–7 PH stainless-steel sheet, Condition TH-1050. Etched with Marble's reagent. X 500

The annealed condition, or Condition A, is attained by heating to 1950 ± 25° F for at least 3 min per 0.1 in. of thickness, then cooling in air. The structure thus produced is primarily austenitic, with little or no transformation to martensite. This structure is shown in Fig. 2.

The austenite can be rendered unstable by heating to 1400 ± 25° F and holding for 90 min. Cooling to 60° F or lower causes transformation to martensite, as in Fig. 3. This is known as Condition T.

Reheating of this transformed material to 1050 ± 10° F for 90 min, followed by air cooling, causes precipitation of the hardening constituent. The steel is now considered to be in Condition TH-1050 (Fig. 4).

A comparison of typical mechanical properties for each condition is given in Table 2.

Other hardening treatments are available for 17–7 PH, but are beyond the scope of this paper.

## Welding Equipment

The welding machine used for the investigation was a 200-kva air-operated circular seam welder which employed direct energy supplied by a frequency-converter type ignitron control operating on a 440-v, 3-phase power source. The maximum electrode force available was approximately 4500 lb. Electrode rotation was continuously variable over a wide range which resulted in welding speeds from about 5 ipm to a maximum of 32 ipm. The electrodes were internally cooled RWMA Class II alloy, 10 in. diam, with a $^{9}/_{16}$-in. face width and machined to a face radius of 3 in. The internal coolant for the welding machine was supplied from a closed system in which the temperature was controlled by mechanical refrigeration. External flood cooling of the weldments was accomplished with water from the plant supply.

## Current Measurement

An effective system for measuring the root-mean-square value of the current flowing in the welding circuit, regardless of its deviation from true sinusoidal form, was considered necessary for several reasons: (a) laboratory investigation requires that a means be at hand for measuring each of the major variables involved in the welding process; (b) it was desirable to be able to compare results obtained through experimentation with those published in the literature for single-phase equipment; (c) suitable current-measuring instrumentation is essential to future research that will have as its objective improved control of the welding process in order to insure proper fabrication of sound welds in critical applications.

Table 2—Typical Mechanical Properties of 17–7 PH Stainless-Steel Sheet

| | *Condition* | | |
|---|---|---|---|
| | *A* | *T* | *TH-1050* |
| Ultimate tensile strength, psi | 130,000 | 145,000 | 195,000 |
| Yield strength at 0.2% offset, psi | 40,000 | 100,000 | 175,000 |
| Elongation in 4T | 35% | 10% | 10% |

Fig. 5 Current wave form in secondary circuit of 3-phase frequency-converter seam welder

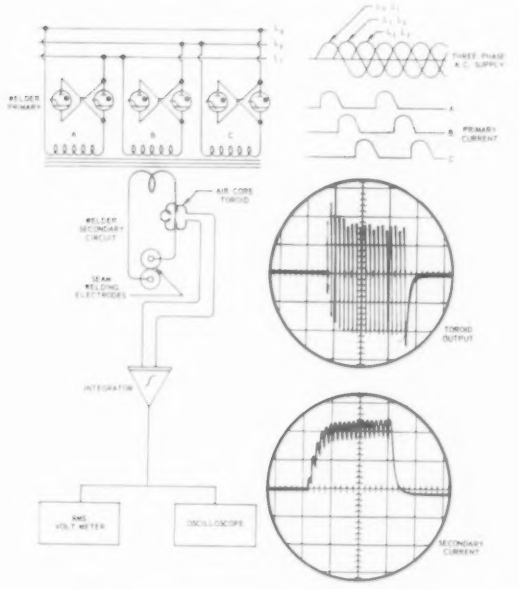


Fig. 6 Instrumentation arrangement and wave shapes associated with various points in the circuit

### Theory

The approach selected for measuring current was based on an experiment made by Oersted in 1820, in which he showed that variation of current flowing in a conductor produces a corresponding change in the magnetic field which surrounds it. An air core toroid was placed around the secondary current conductor of the welding machine and, in accordance with Faraday's Law, the variation in magnetic field surrounding the welding-current conductor induced an emf in the toroid which was proportional to the rate of change of current in the welding circuit. The above relationships are expressed mathematically as follows:

$$\phi = Ki \tag{1}$$

and

$$e_t = N\frac{d\phi}{dt} \tag{2}$$

where

$\phi$ = magnetic flux surrounding the welding-current conductor and linking the toroid.
$i$ = instantaneous value of the welding current.
$e_t$ = instantaneous value of the emf induced in the toroid.
$t$ = time.
$N$ and $K$ = proportionality constants.

By substitution of eq 1 in 2 and combining constants $N$ and $K$ into $R$, we have:

$$e_t = R\frac{di}{dt} \tag{3}$$

The output of the toroid was connected to an electronic integrator whose output could be expressed mathematically as follows:

$$i = \frac{1}{R}\int e_t\,dt \tag{4}$$

Figure 6 is a representation of the welding current wave form commonly encountered when making leak-tight seam welds between continuously rotating electrodes with direct energy, frequency converter-type welding equipment supplied from a 3-phase power source.

The current is seen to comprise undirectional pulses of energy, called heat pulses, which alternate in polarity after time lapses known as cool times. In essence, the current wave form for a leak-tight-type seam weld amounts to an alternating current whose fundamental frequency is determined by the selection of the heat and cool times. The periodic repetition noted above allows the application of the integrator output signal to a true root-mean-square (rms) meter as the final step in determining the effective value of the welding current. The function of the rms meter may be described mathematically as follows:

$$I = \sqrt{\frac{1}{T}\int_0^T i^2\,dt} \tag{5}$$

where

$I$ = effective value of the welding current.
$T$ = period of the seam welding current function as defined in Fig. 6.

Since the current wave form can be assumed to be symmetrical about its zero axis, eq 5 may be written so as to integrate between the limits of 0 and $T/2$.

$$I = \sqrt{\frac{1}{T/2}\int_0^{T/2} i^2\,dt} \tag{6}$$

### Integrating-Period Correction Factor

The root-mean-square value yielded by eq 6 and read on the meter involves an integration which takes place over the duration of the sum of the heat and cool times. For the purpose of the welding investigation, the effective current value of the individual welding impulses defined as heat time was the consideration of importance and a correction factor was developed mathematically.

Observing that nothing is contributed to the integral of eq 6 by the cool time, the expression for the meter reading may be written as:

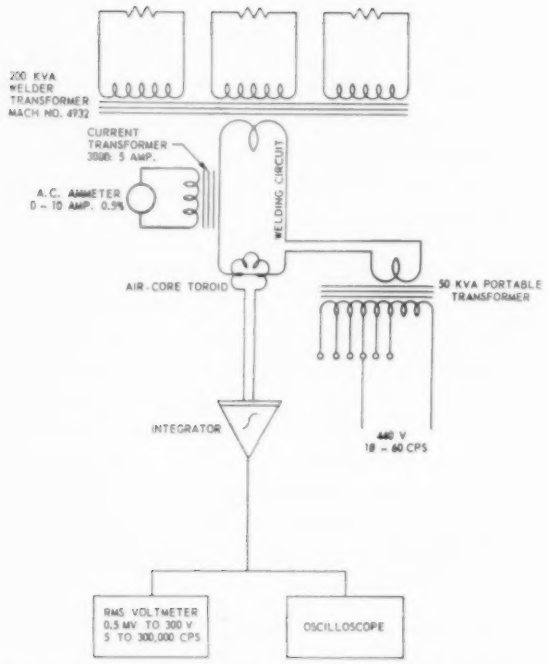


**Fig. 7 Calibration circuit for current-measuring instrumentation**

$$I = \sqrt{\frac{1}{T/2}\int_0^H i^2\,dt} \qquad (7)$$

and by definition in Fig. 6,

$$T/2 = H + C \qquad (8)$$

where

$H$ = heat time.
$C$ = cool time.

The expression for the effective value of the heat time pulse ($I_H$) may be written as:

$$I_H = \sqrt{\frac{1}{H}\int_0^H i^2\,dt} \qquad (9)$$

Using eq 7, 8 and 9, an expression for the effective value of the weld heat pulse may be developed in terms of the actual reading of the rms meter as follows:

$$\frac{I_H^2}{I^2} = \frac{\dfrac{1}{H}\displaystyle\int_0^H i^2\,dt}{\dfrac{1}{H+C}\displaystyle\int_0^H i^2\,dt}$$

$$I_H = I\sqrt{\frac{H+C}{H}} \qquad (10)$$

## Current–Measuring Equipment

The arrangement of the various components of the current-instrumentation system with respect to the principal circuitry of the welder is shown in Fig. 7. The toroid was made from Number 18 copper wire wound on four pieces of 1-in. diam phenolic tubing. The individual coils were assembled around the secondary conductor of the welder and permanently located by a generous application of an epoxy resin.

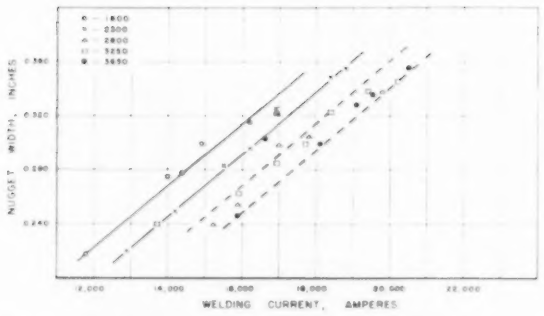


**Fig. 8 Effect of welding current on nugget width in Group C specimens with several values of electrode force**

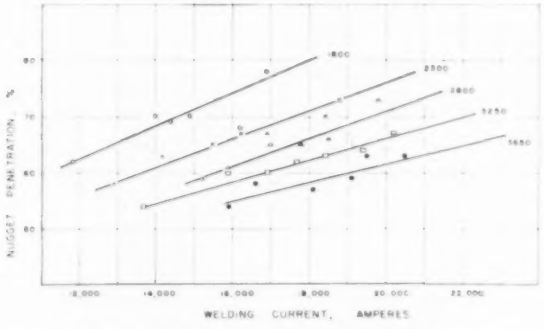


**Fig. 9 Effect of welding current on penetration in Group C specimens with several values of electrode force**

The choice of an amplifier for use as an integrator was governed principally by the availability of a high-grade d-c amplifier which could be modified to provide the feedback necessary to perform the integration of the toroid output. The final selection made use of the vertical amplifier of an oscilloscope.

The root-mean-square meter was an amplifier-detector type instrument comprising an amplifier and a square-law detector which fed a moving-coil meter. The circuitry of the rms meter employed practices well established in analog computers and should not be confused with average and peak-responding type meters which read in rms values only when the wave form is sinusoidal.

Figure 7 also shows the various wave forms present at several points in the system from the 3-phase power supply to the integrator output. The toroid and integrator wave forms in Fig. 7 are reproductions of oscillograms and are typical of the wave forms obtained when using a heat setting of six cycles.

### *Calibration*

The relationship R of toroid output voltage to the time rate of change of current flowing in the welding circuit was determined by causing a known sinusoidal current to flow in the welding circuit and observing the toroid output. The arrangement of apparatus used for calibration is shown in Fig. 8.

It was a requirement that the calibration be accomplished without causing an undue demand on the plant power supply which might interrupt production on

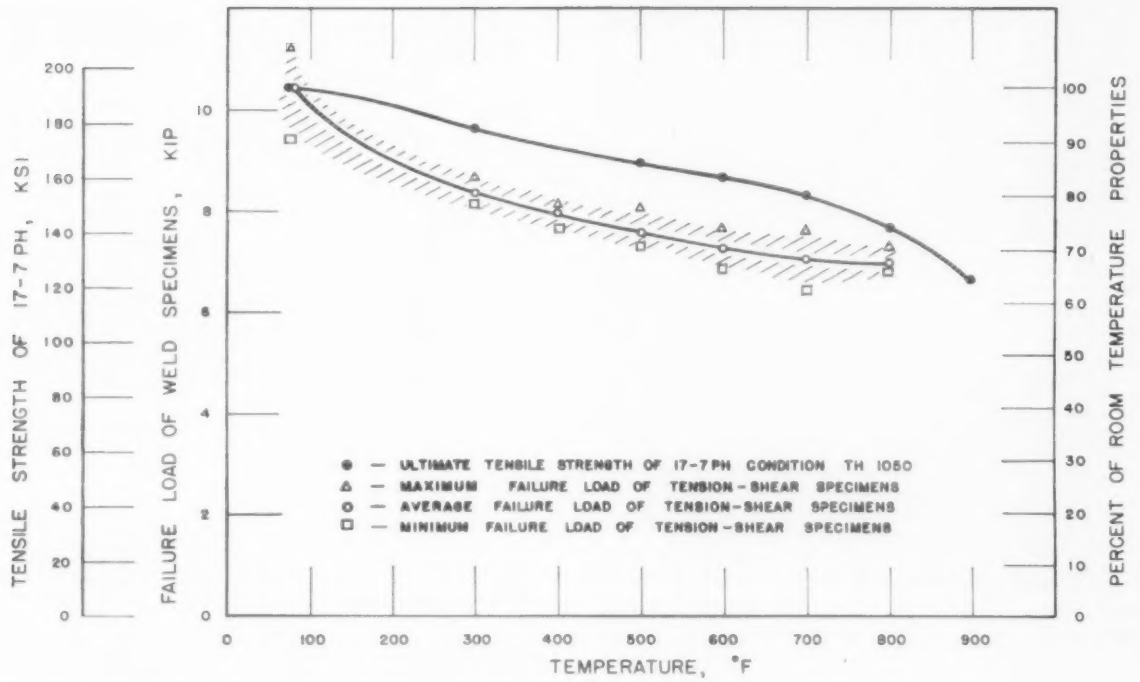
TENSILE STRENGTH OF 17-7 PH, KSI
FAILURE LOAD OF WELD SPECIMENS, KIP
PERCENT OF ROOM TEMPERATURE PROPERTIES
TEMPERATURE, °F
● — ULTIMATE TENSILE STRENGTH OF 17-7 PH CONDITION TH 1050
△ — MAXIMUM FAILURE LOAD OF TENSION-SHEAR SPECIMENS
○ — AVERAGE FAILURE LOAD OF TENSION-SHEAR SPECIMENS
□ — MINIMUM FAILURE LOAD OF TENSION-SHEAR SPECIMENS

Fig. 10 Comparative effect of temperature on strength of tension-shear specimens and base metal

nearby machines. For this reason, a portable 50-kva single-phase spotwelder transformer equipped with a magnetic contactor was used to energize the circuit being calibrated. The calibrating current was measured by means of an ammeter having 0.5% accuracy at its full scale reading of 10 amp. The ammeter was connected to a 3000-amp current transformer in the secondary circuit of the welder. Figure 1 shows the physical arrangement of the instruments, including the ammeter and current transformer used for calibration. The oscilloscope at the right was used as an integrator, the one at the left to monitor both the integrator and toroid output signals.

### *Frequency Response of the RMS Meter*

The frequency range of the rms meter as given by its manufacturer was 5 to 500,000 cps and it was expected that a correction factor to compensate for frequency response would be necessary since several of the combinations of heat and cool times to be investigated would result in a fundamental frequency on the order of 2 cps. The rms values of several weld heat pulses were computed by graphical means and are compared in Table 3 with meter readings to which the integrating period correction factor had been applied. The computed results failed to indicate that any attenuation of meter readings occurred as a result of frequency response.

The periodic function represented by the current wave may be written in the form of a Fourier series, from which an analysis of the relative magnitude of the various harmonic components can be made. The

Table 3—Relationship Between Computed Values of Weld Current and Meter Readings for Several Combinations of Heat and Cool Times

| *Heat time, cycles** | *Cool time, cycles** | *Computed current, amp* | *Measured current, amp* |
|---|---|---|---|
| 6 | 4 | 13,500 | 13,900 |
| 6 | 8 | 10,300 | 10,380 |
| 6 | 10 | 9,420 | 9,750 |

* Cycles of 60 cps current.

function encountered in this work was simplified by its periodic and symmetrical nature which eliminated constant terms and even harmonics; it may be expressed as follows:

$$i = C_1 \sin(\alpha + \theta_1) + C_3 \sin(3\alpha + \theta_3) + C_5 \sin(5\alpha + \theta_5) + \ldots + C_n \sin(n\alpha + \theta_n) \quad (11)$$

where the constants $C_1$, $C_2$, $C_3$, . . . $C_n$ are amplitudes of the component waves, and the angles $\theta_1$, $\theta_2$, $\theta_3$, . . . $\theta_n$ are angles measured from the origin to points of crossing from negative to positive values of the component waves. The angle $\alpha$ is time measured in electrical degrees where $T = 360°$. The constants for the fundamental, third and fifth harmonics of the wave whose current values are shown in Table 3, corresponding to a heat and cool time combination of 6 and 8 cycles, were determined by a graphical method and substituted in eq 11, as follows:

$$i = 8220 \sin(\alpha + 47.20°) + 3495 \sin(3\alpha + 119.05°) + 795 \sin(5\alpha + 347.73°) + \ldots$$

An expression for the rms value of eq 11 may be written as follows:

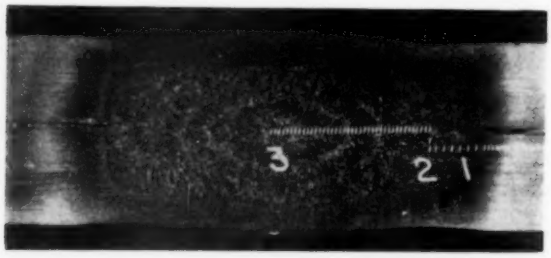


Fig. 11 Transverse cross section of 17–7 PH stainless-steel seam weld, Group C, showing hardness traverse and location of succeeding photomicrographs. Etched with Marble's reagent. X 6

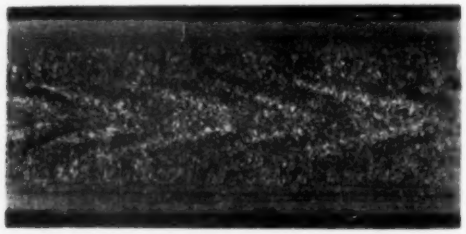

Fig. 12 Longitudinal cross section of 17–7 PH stainless-steel seam weld, Group C. Etched with Marble's reagent. X 6

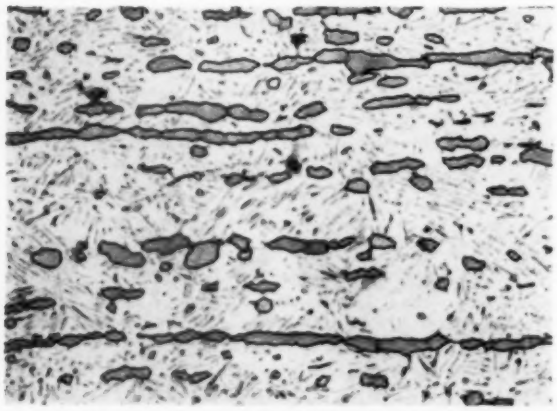

Fig. 13 Microstructure of 17–7 PH stainless-steel sheet in heat-affected zone, near nugget. (Area 1 in Fig. 16.) Etched with Marble's reagent. X 1000

$$I = \sqrt{I_1^2 + I_3^2 + I_5^2 + \ldots} \qquad (12)$$

where $I_1$, $I_3$ and $I_5$ are the effective values of the harmonic components of the wave. By re-writing eq 12 to include a term $Q^2$, which represents the sum of the squares of the remaining components, and then substituting values for the known harmonics and meter readings, it can be shown that

$$I = \sqrt{I_1^2 + I_3^2 + I_5^2 + Q^2}$$

or

$$6950 = \sqrt{\frac{(8220)^2}{2} + \frac{(3495)^2}{2} + \frac{(795)^2}{2} + Q^2}$$

and

$$Q = 2881 \text{ amp} \qquad (13)$$

so the minimum value which could be represented

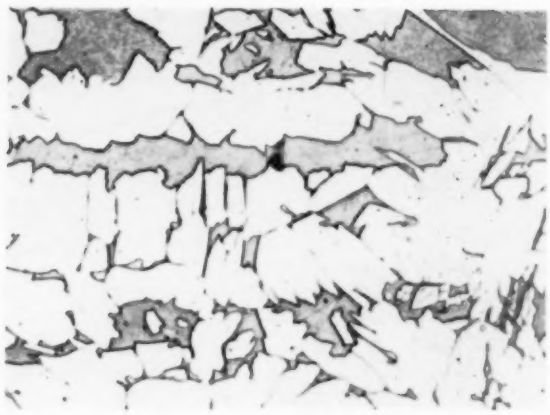

Fig. 14 Microstructure of 17–7 PH stainless-steel sheet, in edge of nugget. (Area 2 in Fig. 16.) Etched with Marble's reagent. X 1000

Fig. 15 Microstructure of 17–7 PH stainless-steel sheet, in center of nugget. (Area 3 in Fig. 16.) Etched with Marble's reagent. X 1000

by harmonics of higher order than the fifth would represent approximately 42% of the meter reading. If the assumption were made that the function could be defined by four terms of the Fourier series, it is obvious that the value obtained for $Q$ would represent the effective value of the seventh harmonic.

A conservative estimate of the maximum error introduced by attenuation due to frequency response of the rms meter can be made, since the response of the meter was determined for sine waves and found to read only 84% of the known value at a frequency corresponding to the fundamental frequency of 2 cps. This estimate is made by substituting into eq 12, 84% of the fundamental and 100% of each of the remaining components, since their frequencies were above the range in which meter readings were adversely affected. Comparing the value obtained with the computed value for the wave,

$$I_A = \sqrt{0.84I_1^2 + I_3^2 + I_5^2 + Q^2} =$$

$$\sqrt{(28.38 + 6.11 + 0.316 + 8.30) \times 10^6} = 6566 \text{ amp}$$

where $I_A$ is the effective value of the current as affected by attenuation due to frequency response of the rms meter. The maximum error introduced into the current measurement system is thus seen to be 5.5%.

## Material and Specimen Preparation

All material was received in Condition A, 0.090 and 0.100 in. thick. This material was sheared to 1- x 5-in. strips for welding samples, and to 5- x 18-in. sheets for welding and tensile-specimen preparation.

These strips and sheets were divided into three groups to be processed in the following sequences:

Group A—Weld in Condition A, heat treat to Condition TH-1050

Group B—Heat treat to Condition T
Descale
Weld
Age to Condition TH-1050

Group C—Heat treat to Condition TH-1050
Descale
Weld

Two methods of descaling were used, alundum grit blasting and chemical descaling and passivation. The chemical processing was performed at 130° F, for 7 min maximum, in the following solution:

Nitric acid, 15–20%
Hydrofluoric acid, 3–5%
Tap water, balance

All samples and specimens were assembled and welded to include one sheet of each thickness, 0.090 and 0.100 in.

The 5- x 18-in. sheets were welded with an overlap of 0.63 in. The welded assemblies were cut into strips perpendicular to the weld joint, and each strip was milled to a 1-in. width for tensile-shear testing.

Sections for microscopic investigation were taken from the weld samples and the 18-in. welded assemblies in both the longitudinal and transverse directions.

## Results

### Welding Speed, Heat Cycling and Surface Preparation

Preliminary welding of samples was done with welding speeds ranging from 18 to 32 ipm, and heat and cool times varying from 8 and 10 cycles to 4 and 3 cycles. Satisfactory welds were obtained under all conditions but an optimum combination of surface condition and weld spacing was produced with a speed of 25 ipm, heat time of 5 cycles, and cool time of 4 cycles.

No significant difference was noted between specimens descaled by grit blasting and those which were chemically descaled and passivated.

Table 4—Room-Temperature Strength of Seam-Welded Joints in 17-7 PH Stainless-Steel Sheet

(Sheet Thickness 0.090 and 0.100 in. for All Specimens)

| Group | | Failure load on 1-in. joint, lb | Number of specimens tested |
|---|---|---|---|
| A (Welded in Condition A, heat treated to TH-1050) | Minimum | 3,525 | 10 |
| | Maximum | 6,870 | |
| | Average | 5,520 | |
| B (Welded in Condition T, aged to TH-1050) | Minimum | 9,380 | 20 |
| | Maximum | 12,010 | |
| | Average | 10,840 | |
| C (Welded in Condition TH-1050) | Minimum | 9,400 | 10 |
| | Maximum | 11,200 | |
| | Average | 10,430 | |

### Nugget Width and Penetration

Nugget width in all tension-shear specimens was greater than the 0.240-in. minimum required by Military Specification MIL-W-6858A, and penetration was between 20% and 80% of sheet thickness.

A study of Fig. 13 shows that the specified nugget width was obtained over a very wide range of welding current and electrode force. Thus, control of these two variables was not critical in the production of an acceptable weld.

Penetration was found to be even less sensitive to variations in welding current and electrode force. Figure 14 indicates that acceptable penetration was achieved with every combination of current and force which was used.

Figures 13 and 14 further show that nugget width and penetration could be readily increased or decreased, simply by adjustment of welding current or electrode force.

### Tension-Shear Testing

Room-temperature strength of seam welds was found to be approximately the same in Groups B and C, these joints developing from 58% to 74% of the guaranteed strength of the steel. These figures are based on a theoretical failure load of 16,200 lb for a tensile specimen 0.090 x 1.00 in. in cross section and with a tensile strength of 180,000 psi. Strength of seam welds in Group A was considerably less, with joint efficiencies of only 22% to 42%. Data on these tests are listed in Table 4.

Seam-welded joints in Group C were tested in tension shear after being radiant heated at the rate of 2.5° F/sec and held at temperature for 1 min before load was applied. Under these conditions, the welded joints were found to have decreasing strength with increased temperature.

Loads required for failure of welded joints, at temperatures to 800° F, were plotted in Fig. 15. For comparison, typical ultimate tensile-strength values for 17-7 PH stainless-steel sheet, from data published by the steel supplier, were plotted at corresponding temperatures.

Loss of strength in the welded joints at elevated temperatures was significantly greater than in the base metal. At temperatures from 300 to 700° F, the welded joints were consistently 12% further below room-temperature strength than was the unwelded steel sheet.

### Metallography

The weld nuggets in these specimens were not clearly outlined. The columnar, or dendritic, type of

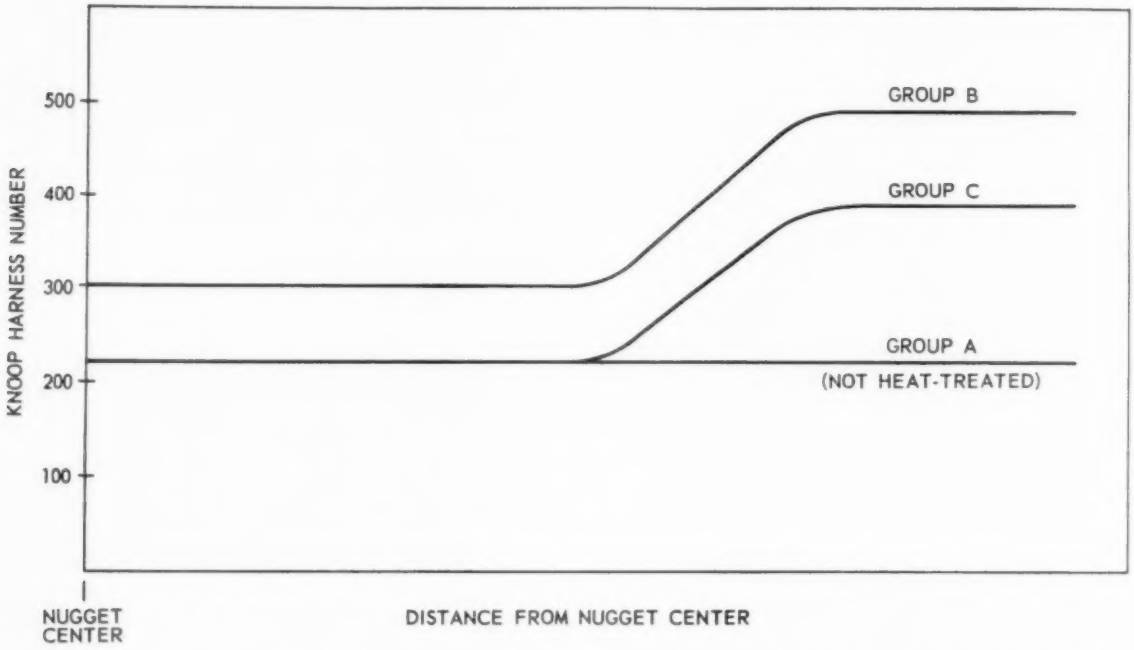


Fig. 16 Idealized comparison of hardness traverses on transverse-seam-weld cross sections in 17–7 PH stainless-steel sheet

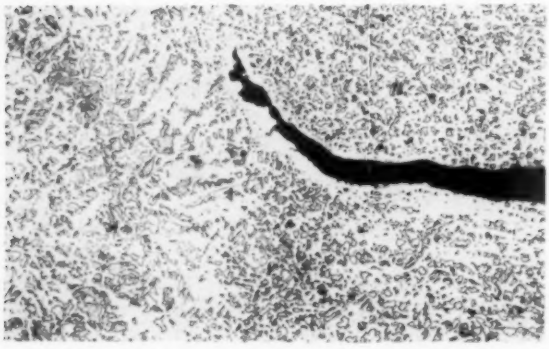

Fig. 17 Transverse cross section of portion of seam weld in 17–7 PH stainless-steel sheet, showing apparent defect. Etched with Marble's reagent. X 50

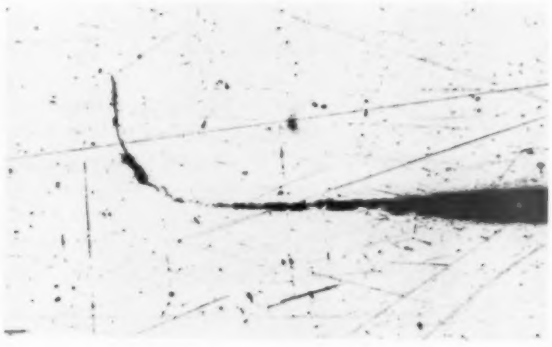

Fig. 19 Oxide inclusion in seam-weld nugget of 17–7 PH stainless steel. Not polished or etched. X 100. Reduced by 25% upon reproduction)

Fig. 18 Intergranular oxidation at face of 0.090-in. 17–7 PH stainless-steel sheet. Not polished or etched. X 1000

structure usually associated with weldments or other cast metals was not commonly found. Instead, the nugget was distinguished primarily by the disappearance of a directional effect in the microstructure.

The cross sections in Figs. 16 and 17 were representative of the weldments produced, with a heat-affected zone surrounding the nugget.

The structure in Fig. 18 is typical of that found in Group C material, in the zone adjacent to the nugget, the ferrite being aligned in the direction of sheet rolling in a matrix of hardened martensite.

Figure 19 represents a section at the edge of the nugget, the ferrite having lost much of its directional appearance; the matrix is considered to be primarily austenite, with some martensite.

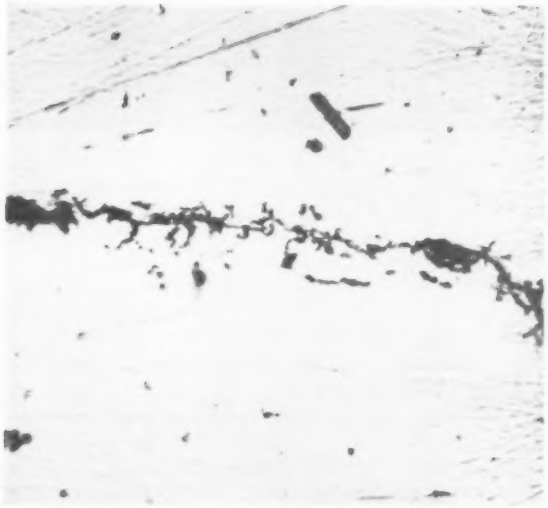

Fig. 20 Portion of oxide inclusion shown in Fig. 10. X 500

The center of the nugget has the appearance shown in Fig. 20, all directional tendency having been eliminated.

A Tukon hardness traverse was run on each of three specimens: one from Group A before heat treatment, and one each from Groups B and C. The results of this traverse are shown, in idealized form, in Fig. 21. Virtually identical hardness was noted in the nuggets of the Group A and C specimens; significantly higher hardness was found in the nuggets of Group B specimens, indicating that some hardening had occurred in the nugget during aging.

### *Welding Defects*

The seam welds produced were found very nearly free of welding defects, regardless of variation in welding conditions. X-ray inspection revealed some expulsion and porosity at the beginning of each weld, but very little evidence of porosity at any other point. Study of a large number of metallographic sections confirmed this freedom from defects.

Discontinuities resembling cracks were found in some nuggets of Groups A and C. Such an apparent

Fig. 21 Portion of oxide inclusion shown in Fig. 10. X 500

defect is shown in Fig. 5. Investigation revealed that the 0.090-in. sheet used in Groups A and C had been re-annealed at a temperature higher than recommended, producing an intergranular oxidation, as seen in Fig. 9. The oxide thus formed was not removed during descaling, and tended to move within the molten steel during welding. A cross section of this type of oxide inclusion, before final polishing of the specimen, is shown in Figs. 10, 11 and 12.

*Acknowledgment*

The authors wish to express their appreciation to R. W. Ludwig, chief of Engineering Laboratories, and L. H. Vaughan, test engineer, Electronics and Environmental Laboratory, Temco Aircraft Corp., for their assistance in developing and calibrating the instrumentation for measurement of welding current.

*Reference*

Military Specification MIL-W-6858A, "Welding: Aluminum, Magnesium, Non-Hardening Steels or Alloys, and Titanium, Spot, Seam, and Stitch," amended July 9, 1957.

# $CO_2$-SHIELDED ARC WELDING BOOSTS PRODUCTION OF AUTO FRAMES

The cost-cutting potential of $CO_2$-shielded metal-arc welding is being used to a good advantage by the Parish Pressed Steel Division of Dana Corp., Reading, Pa., in the production of mild-steel automotive-frame subassemblies (See Figs. 1 through 3).

Based on a story by Hobart Brothers Co., Troy, Ohio.

Using an air-cooled semiautomatic gun, production welding speeds greater than 200 ipm have been obtained by the manufacturer on ideally positioned fillet welds.

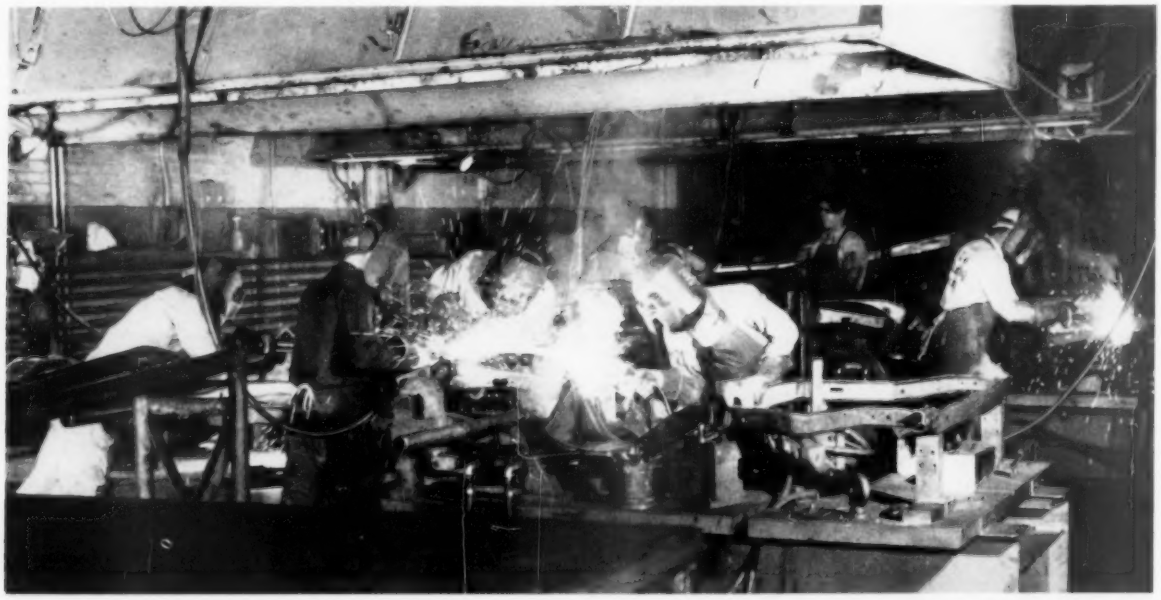

Fig. 1 Two men in center of fixture in foreground use air-cooled guns to weld junction plates to front and rear frame subassemblies with $CO_2$-shielded arc-welding process

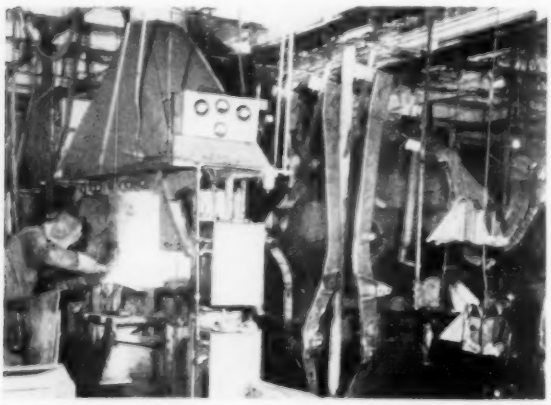

Fig. 2 Front inner and outer sidebars being joined to form box-section siderail. Two operators work simultaneously

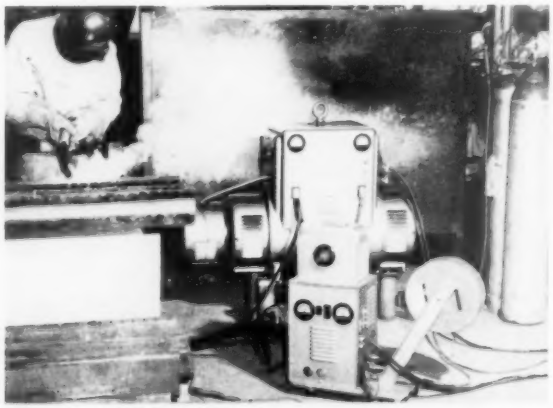

Fig. 3 As a portable $CO_2$-shielded metal-arc welding unit, the wire feed, controls and wire-reel arm are mounted on a platform which rolls on casters

Auto styling trends to lower bodies, hardtops, no center pillars, etc., have increased the need for stronger frames. Consequently, more box sections are required to produce the stronger frames and the number of inches of weld per frame has increased tremendously. It was impractical and costly to add sufficient manpower and equipment to cover the increase by conventional welding methods.

Much experimental work was done with semiautomatic gas-shielded metal-arc welding, using argon, helium and $CO_2$ as an arc shield. Welding speeds were increased radically, but suitable equipment had to be used for the rugged work of frame welding.

According to manufacturer, the semiautomatic $CO_2$ shielded-arc welding with the air-cooled gun solved the majority of their problems.

# WELDING COSTS REDUCED BY TWO-THIRDS IN FABRICATION OF CABLE-DRUM HEAD

The Wellman Engineering Co., Cleveland, Ohio, has reduced welding costs by as much as two-thirds in the fabrication of a variety of work by using a manually-guided automatic submerged-arc welder (Fig. 1).

A typical example of the work on which Wellman has used this welding equipment is the head for a large Barney Hoist cable drum. The head is essentially a 7-ft diam disk of $1\frac{1}{2}$-in. thick steel plate with a large hub in the center and 1-in. thick wedge-shaped stiffeners radiating from the hub toward the periphery of the disk. Welds include a $\frac{3}{8}$-in. fillet all the way around each stiffener, joining it to the plate, and a $\frac{5}{8}$-in. fillet over the scarfed joint between plate and hub.

Three facts contributed to the savings in fabricating costs. First, welding speeds were twice as fast as those possible with manual welding. Second, the penetration of the submerged-arc process eliminated beveling on the stiffener and reduced bead size. Finally, the operating factor was well above that obtainable with manual welding.

Here's how they welded the job. They positioned the plate flat and blocked it up about 3 ft off the floor. The hub and stiffening ribs were already tack welded in place. In this position, the welds became one continuous bead running out around each rib and back to the joint between hub and plate. The operator set the welding current to 550 amp, the arc voltage to 37 v, and the travel speed to 17 ipm. He welded continuously for 8 to 10 min without stopping and completed the bead around two stiffeners. There, simply as a relief from continuous welding, he stopped and removed the slag from the bead. This was done quickly, since the slag had already peeled itself from most of the weld. Then, he continued welding.

The total time for completely welding and cleaning the 30 ft of $\frac{3}{8}$-in. fillet was less than half an hour.

Fig. 1 Manually-guided automatic submerged-arc welder in operation

Based on a story by the Lincoln Electric Co., Cleveland, Ohio.

# GAS-SHIELDED METAL-ARC SPOT WELDING OF RAILROAD CABOOSES

BY B. J. YELIN, M. J. BAUGHMAN AND R. E. STENTZ

The gas-shielded metal-arc process—adapted for spot welding with a special nozzle and timing device—is used to make 456 spot welds on each caboose fabricated by the International Railway Car Co., Kenton, Ohio (Figs. 1 through 3).

Seven sheets of 14-gage copper-bearing mild steel make up the inside lining of one side of the caboose. These sheets are butted together over "Z" bars. Spot welds are made at this seam 6 in. apart for a total of 14 spots per seam. The previous method, which caused warpage, was to make a continuous automatic weld.

Spot welds are also made 12 in. apart on stiffeners formed from 14-gage steel in the form of deep channels, replacing a drilling operation and the use of metal screws. In addition, spot welds are made across the top section 6 in. apart and across the bottom section 12 in. apart.

Each spot weld requires three sections and 3 in. of $^1/_{16}$-in. wire within a shield of argon gas plus 3% oxygen. To obtain uniform spacing of spot welds, templates made of plywood with holes drilled at proper intervals are used. This also serves to hold the sheets down during the welding operation.

B. J. Yelin is vice-president, International Railway Car Co.; M. J. Baughman is technical sales representative and R. E. Stentz is sales representative, Air Reduction Sales Co., Detroit, Mich.

Fig. 1 Gas-shielded metal-arc spot welding was used to fabricate this caboose

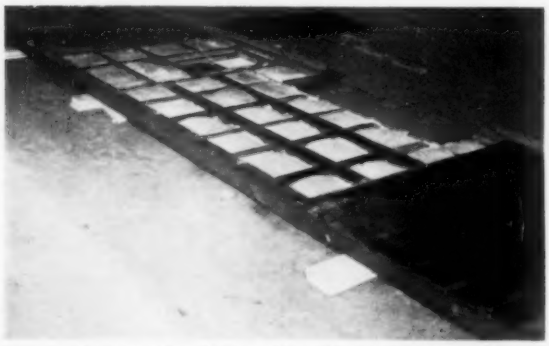

Fig. 2 Section of caboose side with steel sheets removed to show construction

Fig. 3 Gas-shielded metal-arc spot-welding equipment being applied. 456 spot welds are made on each caboose

# FROM THREE DAYS...
# ...TO THREE HOURS

Two submerged-arc welding heads, each operating independently, have cut operating time for rebuilding links on "Caterpillar" (D-8) track from three days to three hours. Missouri Valley Machinery Co., Omaha, Neb., now completes the rebuilding job eight times faster than with the manual metal-arc welding method used previously.

A unique mounting arrangement is required to utilize the two submerged-arc welding heads. Each head is hung on a separate side-beam carriage—permitting the links to be welded on both sides simultaneously as the two heads pass over the track. Cams, located on the back of each beam, trip a switch which controls welding over each link. Power settings are 30 v and 400 amp, ACHF.

Based on a story by the Linde Co., New York, N. Y.

Two layers of hard surfacing are applied with $^{3}/_{16}$-in. diam wire, requiring ten passes over each side of the track to produce a $^{3}/_{16}$-in. thick deposit. If the links have worn excessively, a mild-steel wire with 0.65% carbon content, $^{3}/_{16}$ in. diam, is deposited before the two layers of hard surfacing are applied. Number 80 welding composition—recommended especially for build-up operations—is used because it does not add excess silicon and manganese to the weld metal in multi-pass welding.

With the two submerged-arc welding heads, it takes only 18 minutes to make complete passes on each side of the track—measuring 27-ft long and containing 39 links. As a result, it takes only three hours operating time to rebuild one track. It took about 24 hours for one man to complete the same job using manual metal-arc welding.

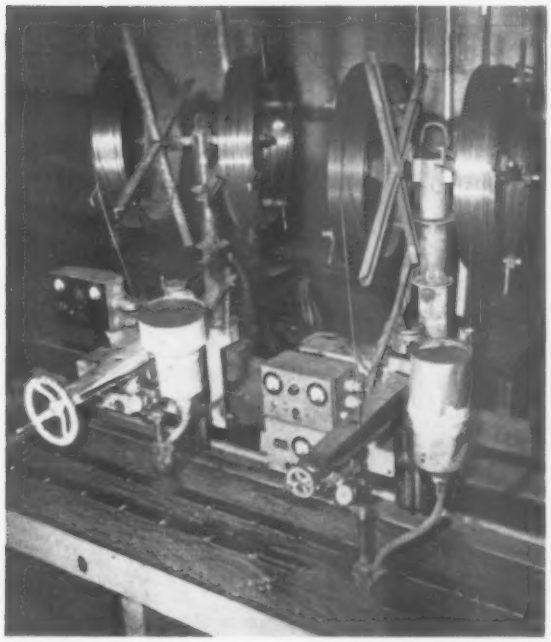

Fig. 1 Two submerged-arc welding heads, mounted on separate side-beam carriages, operate independently . . . and simultaneously . . . to rebuild "Caterpillar" (D-8) track

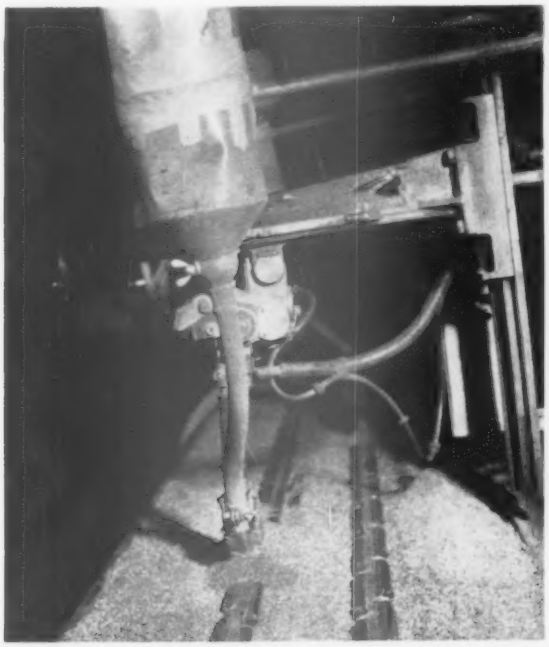

Fig. 2 A close-up of one of the submerged-arc heads in operation. Notice the smooth, even surface of the deposited metal

# Society News

## AWS National Headquarters to Conduct Information Center

At a recent meeting of the Board of Directors of the AMERICAN WELDING SOCIETY, formal approval was given for the creation of a new department within the framework of national headquarters in New York City. This new group will be known as the Information Center and its prime objective will be to introduce the public to welding, and to inform them of the safety, strength and economy of welded structures.

Such a function within the SOCIETY has been under study for several years, particularly by a subcommittee on welding information chairmaned by 1st Vice-President C. I. MacGuffie. It was finally decided that the new Information Center would be under the guidance of the AWS Publicity Committee of which A. V. Scherer is chairman. The full-time staff representative for the department will be A. L. Phillips.

The official goal of the Information Center will be "to further the objectives of the AMERICAN WELDING SOCIETY and create a favorable climate for welding by supplying accurate, newsworthy and timely information on welding and related processes to the widest possible public, as well as engineering groups who are unfamiliar with the latest welding techniques, in order to make known the availability of the SOCIETY'S tremendous wealth of technical information."

This latest of AWS functions will disseminate developments in the field of welding through a clearing-house procedure and direct this information via international, national and local channels of communication. Welding information will be sent to such media outlets as newspapers, magazines, company house organs and radio and television stations. Other planned duties of the Information Center will be to accumulate statistics, training curricula and data on available speakers and films. Studies of industrial trends are also in the planning stage.

The Publicity Committee points out that a program of this nature cannot develop fully within a short period of time. The entire membership of the AMERICAN WELDING SOCIETY can help make this Information Center a success by transmitting unusual welding developments of local origin to AWS national headquarters for study and possible publicity.

### AWS DIRECTORS-AT-LARGE

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|---|---|
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| Junior Past-President J. J. Chyle | |

### National Nominating Committee Formed by AWS

The National Nominating Committee for the 1958–59 fiscal year has been recently constituted. This group will nominate the officers for the AWS 1959–60 term. C. P. Sander is chairman and other members will include J. E. Dato, L. C. Bibber and T. B. Jefferson. Representation from the various districts includes H. Turek (No. 1), C. L. Kreidler (No. 2), B. C. Motl (No. 3), T. R. Berg (No. 4), R. B. McCauley (No. 5), R. H. Hoefler (No. 6), L. L. Baugh (No. 7), L. N. Williams (No. 8), R. D. Spraque (No. 9), A. H. Butler, Jr. (No. 10), and W. R. Smith (No. 11).

## Two Baton Rouge Firms Enrolled as Supporting Companies

Two more firms have been enrolled as Supporting Company members in the AMERICAN WELDING SOCIETY. They are Action Welding Supply Co., Inc., 5373 Choctaw Drive, and Cal-Metal Pipe Corp. of Lousiana, Mengel Rd. at L&A Railroad. Both memberships for these Baton Rouge, La. firms were effective on Aug. 1, 1958.

## Sciaky Enrolled as Supporting Member

The latest Supporting Company enrollment in the AMERICAN WELDING SOCIETY is the firm of Sciaky Bros., Inc., 5915 W. 67th St., Chicago 38, Ill.

This is in addition to the Supporting Company membership of Sciaky's Los Angeles office and their Sustaining membership.

**INSTRUCTIONS**

**FOR**

**PROSPECTIVE AUTHORS**

Authors who plan to submit papers for publication in THE WELDING JOURNAL are invited to send for a free copy of the booklet "Instructions and Suggestions for Authors."

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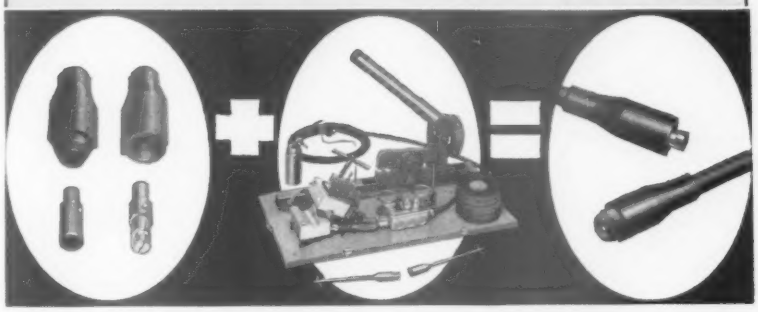

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# THINGS YOU SHOULD KNOW

**(That Happened in June and July)**

● On June 2nd, Technical Secretary Fenton, the writer and representatives of ASME and ASA met to discuss joint reproduction and coordinated distribution of the revised Standard Welding Symbols.

● The following day, Mark Haines of Santa Rosa, Calif., visited AWS Headquarters. Mr. Haines joined the Society in 1921 and has been an active and interested member for the past 37 years.

● A joint meeting of the Finance and Executive Committees was held on June 4th and followed the next day by a meeting of the Board of Directors. A balanced budget of income and expense was adopted for the 1958–59 fiscal year, including provision for a new information service with A. L. Phillips in charge and two new staff members. Changes in the Society By-Laws covering committees and the organization structure were approved and will be presented to members in a later edition of the Journal.

● On June 6th President G. O. Hoglund, your Secretary and Staff Member Phillips were guests at the christening of the U.S.S. Independence, one of our largest type aircraft carriers involving fantastic amounts of welding and allied fabricating operations.

● On this same day the entire metal industry was saddened and paid well-deserved tribute as funeral services were held in Cleveland for "Bill" Eisenman, lifetime Secretary of the American Society for Metals. Pioneer and leader in the activities of ASM, Mr. Eisenman was also a member and contributed in many ways to the development of AWS.

● The following week conferences were held with Al Scherer, chairman of the reorganized Publicity Committee which will plan and assist in organizing the new Society Information Service, and with Past President Humberstone concerning headquarters housing in the new engineering center and investment policies covering the reserve and contingent funds of the Society.

● During this same week the Welding Journal Committee held an important meeting at which important policy decisions concerning advertising and subscription rates were considered, with Chairman Tangerman presiding. At the same time Chairman Ted Long conducted a meeting of the Exposition Committee in his usual efficient manner. Reports covering the successful exposition at St. Louis were presented and plans for the April 1959 Exposition in Chicago were completed. Site reports prepared by the Secretary were discussed and it was decided not to hold the 1963 meeting in the Southwest.

● On June 17th Charlie Lytton of Pittsburgh and Ed Wenk of Southwest Research Institute in San Antonio visited headquarters. Lytton requested assistance in contacting New York consulting engineers concerning the use of welded fabrication for two large bridges scheduled for construction in the Pittsburgh area.

● First Vice-President C. I. MacGuffie and C. L. Moss, past chairman of the Dallas Section, were visitors at headquarters the following day. Marshall Holt, associate of President Hoglund, called later in the week to inquire about welding publications in Japan and to seek assistance in securing copies of an article published there.

● During the night of June 20th your Secretary and Mrs. Plummer flew from New York to Brussels for a quick look at the Exposition with its many unique and interesting buildings, including the welded Atomium. The head of the Belgium firm responsible for the fabrication of this unusual structure is a good friend of your Secretary and provided a personal tour including a review of the welding features.

● A few days later your Secretary joined WRC Director Bill Spraragen and more than twenty other representatives of the United States at the annual meeting of the International Institute of Welding in Vienna. About 1000 men and women from some twenty-five nations attended the impressive opening ceremonies addressed by the President of Austria, the Mayor of Vienna, the President of IIW (from Italy) and other important Austrian officials, and later participated in a delightful social program including several receptions, a performance of the famous Spanish Riding School, a Vienese Ball, an Opera and other special musical events.

● Morning and afternoon sessions of the fifteen working commissions kept all delegates busy exchanging information, discussing standards of design and good practise, and attempting to arrive at common recommendations covering all types of welding and allied processes.

● Your Secretary attended a meeting of the Governing Council on the Sunday preceding the formal opening of the conference on Monday morning, and another meeting on the following Saturday. At this final meeting of the Council it was voted to admit the welding societies of Russia as members of the Institute. It was also confirmed that the 1959 meeting of the IIW will be held at a city in Yugoslavia.

● The week following the main conference J. Zimmerman and your Secretary, together with our wives, joined sixty other members of the Congress in an industrial tour through southern and western Austria. Plant visits included Böhler & Co. at Kapfenberg where several hundred kinds of special steel are manufactured; the large steel works of the Österreichisch-Alpine Montangesellschaft at Donawitz where the new "oxygen" process for steelmaking was developed; the important Elin works at Weiz where new welding processes have been developed and large electric generators and transformers as well as welding equipment are manufactured for shipment all over the world; the Waagner-Biró plant at Graz where all types of pressure vessels and many industrial weldments are fabricated; and the gigantic dams, penstocks and power plant of the Tauernkraftwerke at Kaprun.

● The "LD" steel-manufacturing process developed at Donawitz makes use of a "converter" similar to a Bessemer converter except that high purity oxygen is blown from above onto the surface of the molten metal which consists essentially of pig iron with a very low percent of scrap. The resulting product is equal in quality to good open hearth steel. The process shows excellent economy when good scrap is not available or high in cost.

● Dr. Rosenberg became manager of the Elin works in 1919 and is credited with developing the crossfield welding machine. The first arc welded railroad bridge was built near the Elin works in 1921 and our inspection showed it giving good service after 37 years of continuous use. An interesting application of resistance welding was observed in a tube welding machine which automatically forms steel tapes into tubes, welding, cutting, dressing and discharging the tubes continuously.

● The Elin-Hafergut "stick" welding process, in which long electrodes are laid in the welding groove and a cover bar is used, was demonstrated for several special applications.

● Following the industrial inspections your Secretary and Mrs. Plummer flew to Stockholm to start a short vacation in Sweden, Norway and Denmark. We were greeted immediately after arriving at our hotel in Stockholm by Mr. and Mrs. R. D. Thomas, Sr. and enjoyed a delightful evening with them.

● After brief stops in Oslo, Bergen and Copenhagen and a visit to the Norwegian Fjords we flew back to New York late in July, to find a completely renovated office at headquarters and reports of many staff activities completed during our stay in Europe.

*Fred L. Plummer*

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# Educational Projects for 1958-59

by Clarence E. Jackson

In the August issue I dealt largely with foreign aspects of education and touched on the similarities and differences in the various countries. This month I would like to indicate the direction we ourselves are going, the projects we are considering and the additional activities we are undertaking.

The presentation of the Educational Activities Committee at St. Louis, led by T. Embury Jones, vice chairman of EAC, was built around the idea that "Welding Education Means Business." This title has two meanings, but the reports clearly indicated that the AWS has an integrated program under way. The breadth of the activities widens as the membership of the EAC increases. The efforts of Butch Sosnin in the apprentice field, of Orville T. Barnett in in-plant training, of Roy McCauley and Morris Thomas in the college field, of John Light, William Williams, George Luther, J. J. Chyle, Stanley Walter, Richard Wiley, Ray Townsend and all their committee workers will be more and more evident. We have planned a broad general program which will require time to accomplish, but the specific goals will show results much more quickly.

The Educational Activities Committee held a meeting at Louisville, Kentucky, in June to review the previous year's work, make plans and consider assignments for the new fiscal year. Despite the achievements of the past year, there was no sign of complacency. The feeling of the meeting appeared to be "Why can't we do more—faster?"

The new Speakers Directory was discussed and it was disclosed that little in the field of education was included. None of the companies contacted had specified education as a subject. Practically all welding processes had been covered; many titles included metals, applications and the like, but education as a title was conspicuously absent. The committee decided to prepare a list of speakers on various educational subjects of interest to the welding industry for inclusion in the supplement to the Speakers Directory.

We hope to have the supplement available this month.

**Clarence E. Jackson,** chairman of the Educational Activities Committee of AWS, is research metallurgist with the Linde Development Laboratory, Newark, N. J.

## Standards for Welding Instruction in Schools

The *Detroit* Section of the AWS has been working on this project and has prepared a great deal of the material. This has been handed over to the EAC with an offer to help on any committee formed for this project.

There is a widespread need for such information. The lack of such guidance is seriously hampering welding instruction in schools. When new schools are being erected the plans should include adequate facilities for welding instruction. No standards exist either for instruction or for buildings and equipment. There are many sources from which information may be obtained. Unfortunately, each source may come up with a different recommendation.

It is very desirable that some form of standardization be approved and adopted. To this end we are consulting with the American Vocational Association and are suggesting that we work together through the medium of joint committees.

The *Detroit* section has had much experience with this project and suggests the preparation of a booklet similar to that prepared by the American Vocational Association and the Automobile Manufacturers' Association: "Standards for Automotive Service Instruction in Schools."

This standard is an 84-page booklet, fully illustrated, and prepared as a result of a decision to establish standards in automotive training to raise the general level of automotive mechanics' instruction in the public schools of the nation.

We are hoping to obtain the cooperation of AVA and form a joint committee early this fall.

## College Training

Morris Thomas is preparing a questionnaire for distribution to colleges to determine the amount of welding taught. This will provide valuable information for future operations and augment the survey completed in 1950–51. There have been many changes in the field of education these last few years and recent trends indicate the need for another and more recent review of welding in American engineering education.

It is only by means of such a review that we can evaluate the extent and usefulness of welding education in our colleges. It is upon such an evaluation that our recommendations are based. In these fast-moving times it is extremely difficult for education to keep pace with technical demands. Technological developments are more rapid than the preparation of textbooks, since textbooks cannot be written until a development has passed all tests, including that of time.

These are some of the newer projects. Our committees are working on many others which are showing results. We have distributed hundreds of course outlines and many sections have based their educational series on some of these outlines.

The outline for in-plant training has been sent to many companies in response to requests for information on how to train personnel. This outline, prepared by Orville Barnett's committee, is very comprehensive and has received favorable comment wherever it has been sent.

I look forward to a very productive year and, with the cooperation of section educational representatives, anticipate a great increase in all educational activities.

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# Section News and Events

as reported to Catherine O'Leary

## California

### ANNUAL REUNION NIGHT

**Huntington Park, Calif.**—The *Los Angeles* Section held its annual reunion night on Thursday evening, June 19th, at the Elks Club in Huntington Park, with the ladies invited.

This being the last meeting of the fiscal year, technical papers and talks were dispensed with and an outstanding program that would be pleasing to the ladies was arranged.

Cocktails were served on a large open patio, accompanied with music and songs by Miss Sylvia Haines, accordionist. A superb barbecued steak dinner was served to the 129 members, their ladies and guests.

Francis McGinley, past chairman and new director of District No. 10, acted as master of ceremonies for the evening.

Seated at the speakers' table were the following officers, their wives and guests: C. B. Smith, past chairman and past National Membership chairman; John Wiley, treasurer; Al Thompson, secretary; Dick Hayes, vice-chairman; Chairman Dave O'Connor; guest speaker, Tony Cabooch; Francis McGinley; "Sandy" Sander, senior past president, and Charles Babbit, vice-president, Cal-Metal Corp., who was present to receive the annual achievement award.

During the dinner hour, entertainment was furnished by Bob Waters of Pacific Metals Ltd., who sang and was accompanied by Dr. B. G. Kingsley at the piano.

Among the many events that took place was the presentation of the annual achievement award to Charles Babbit of Cal-Metal Corp. for his many contributions to the welding industry.

Highlight of the evening's program was the featured speaker, Tony Cabooch, who was introduced as Dr. Jose Fernando Trigo, a newspaper publisher, author and humorist from Brazil. His highly entertaining talk was entitled, "The Industrial Relations Between the Two Americas." In concluding his talk, Tony gave a résumé of the evening's events which he compiled during the dinner hour.

## SECTION MEETING CALENDAR

### SEPTEMBER 8

WICHITA Section. Howards Foodliner (downstairs). 6:00 P.M. Social Hour. 7:00 P.M. Dinner. 8:00 P.M. "History of Welding," by Roy W. Poe, Tweco Products, Inc.

### SEPTEMBER 9

DALLAS Section. Western Hills Motel, Highway 183, bet. Dallas and Ft. Worth. Keg time 6:30. Dinner 7:00. Program 8:00 P.M. "Tungsten-Arc Welding of Stainless Steel and Titanium" by J. C. Collins of Convair.

### SEPTEMBER 12

PITTSBURGH Section. Plant visitation. 1:30 P.M. Westinghouse Electric Corp. East Pittsburgh Works. 12:45 P.M. Lunch in Cafeteria.

COLUMBUS Section. Plant tour of Arcair Company, Lancaster, Ohio.

### SEPTEMBER 15

PHILADELPHIA Section. Drexel Night. Dinner 6:30 P.M. Cavanaughs Dining Room. Meeting 8:00 P.M. Drexel Institute. "Engineering and Welding" by Albert Muller, Air Reduction Co., Inc.

### SEPTEMBER 16

HARTFORD Section. Villa Maria, Glastonbury, Conn. Dinner 7:30 P.M. Meeting 8:30 P.M. "Heavy Weldments," C. J. Ascenzi and F. P. Iapalucci, Baldwin-Lima-Hamilton Corp.

### SEPTEMBER 22

WESTERN MICHIGAN Section. Plant tour Kirkhof Mfg. Co.

### SEPTEMBER 23

OLEAN-BRADFORD Section. Wellesville, N. Y. "Pressure Vessel Fabrication for Nuclear Vessels," William R. Apblett, Foster Wheeler Corporation.

### OCTOBER 3

PHILADELPHIA Section. Engineers Club Junior Room. Panel Discussion. "Automatic Welding." Moderator, David Buerkel.

### OCTOBER 7

PITTSBURGH Section. Joint meeting with ASCE and ESWP. New Aspects—Welded Bridge Design. "Research-Thin Web Design," Bruno Thurliman, Fritz Engineering Lab., Lehigh University.

### OCTOBER 9

PUGET SOUND Section. Engineers Club, Seattle, Wash. "Welding—Its Application to Rocketry," F. H. Stephenson, Aerojet Corporation.

### OCTOBER 10

DETROIT Section. Engineering Society of Detroit. Dinner 6:15 P.M. Meeting 8:00 P.M. "Practical Aspects of Welding Aluminum in Missiles," William A. Wilson and James K. Dawson, Army Ballistic Missile Agency.

*Editor's Note: Notices for December 1958 meetings must reach Journal office prior to September 20, so that they may be published in November Calendar. Give full information concerning time, place, topic and speaker for each meeting.*

## North Carolina

### WELDING QUALIFICATION PROCEDURES

**Wilmington, N. C.**—The regular meeting of the *Carolina* Section was held on June 27th at the Ark Restaurant in Wilmington.

E. C. Korten (M'43), assistant chief engineer, Boiler Division, Hartford Steam Boiler Inspection and Insurance Co., was the guest speaker for the evening. Mr. Korten's subject was on "Setting Up Welding Procedure and Qualifications." He discussed the seven important steps to be taken to weld under code requirements.

Prior to the meeting a tour was made of the Babcock and Wilcox Co.'s Boiler Plant where the members and guests were shown methods and practices of welding in operation.

## Tennessee

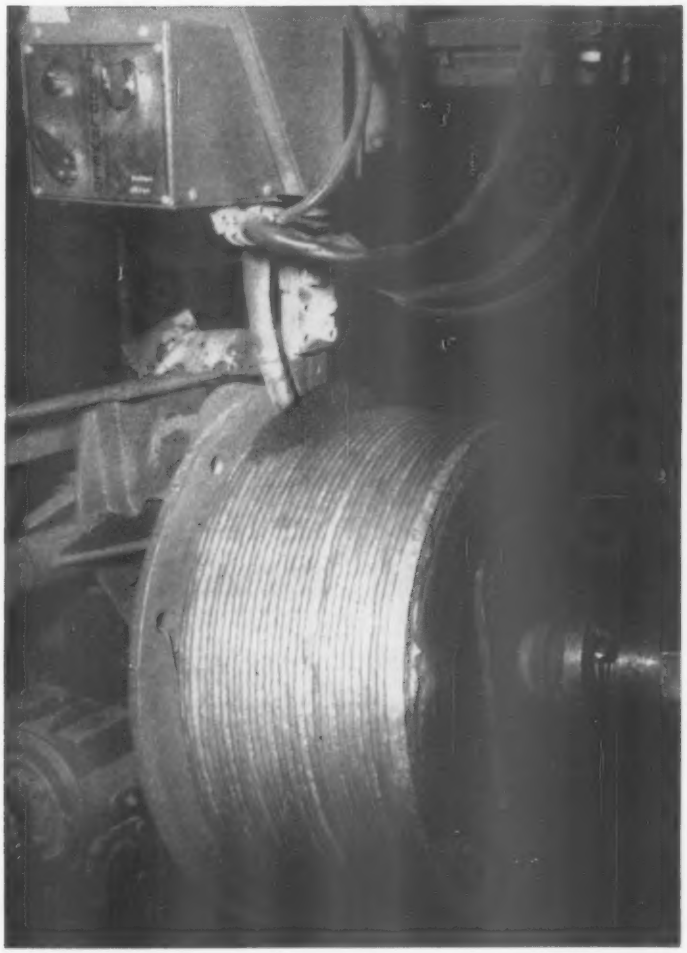
### ANNUAL LADIES' NIGHT

**Oak Ridge, Tenn.**—The annual ladies' night and dinner party of the *Northeast Tennessee* Section was held on the evening of May 16th at the Waldorf Restaurant. Fifty members

(*Continued on page 920*)

# Weldynamics

## ARC WELDING AT WORK CUTTING COSTS



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## ANNUAL REUNION NIGHT FOR LOS ANGELES SECTION

Outgoing Los Angeles Section Chairman Dave O'Connor (*left*) extends his best wishes for success to new chairman, John Ross. Meeting was held June 19th

Past Section Chairman, and 10th District Director, Francis McGinley presents Past-President "Sandy" Sander with oxygen-cutting torch

Here, Charles Babbit, a member of the Los Angeles Section, is presented with annual achievement award by Mr. McGinley

Section Secretary Dick Hayes assists Mrs. O'Connor in unwrapping gift presented to her by Los Angeles Section

Highlighting the evening's entertainment was none other than Tony Cabooch—"Push 'Em Up Tony" of radio fame

72-year-old Dr. B. G. Kingsley accompanies Section member Bob Walters, who sang several songs during the evening

---

## DINNER PARTY HELD BY NORTHEAST TENNESSEE SECTION

A portion of the members and guests attending the Annual Social and Dinner Party held by the Northeast Tennessee Section on May 16th

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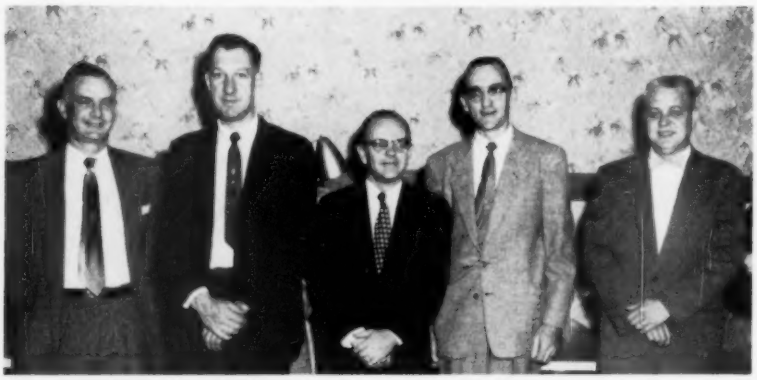
## NORTHEAST TENNESSEE OFFICERS FOR 1958–59



The elected officers of the Northeast Tennessee Section for 1958–59 are (*left to right*) H. B. Harlan, treasurer; C. H. Wodtke, 1st vice-chairman; T. C. Swindell, chairman; Bert Searles, 2nd vice-chairman; H. M. Payne, secretary

(*Continued from page 916*)

and their ladies enjoyed an evening of entertainment and dancing.

The incoming officers for the new year were introduced and a presentation of a past chairman's pin was made to W. J. Leonard by incoming chairman T. C. Swindell.

## SPACE FLIGHT

**Oak Ridge, Tenn.**—The *Northeast Tennessee* Section, in co-sponsorship with the Oak Ridge Technical Societies Joint Council, had the pleasure of presenting Colonel Paul Andrew Campbell of the USAF Medical Corps in an address at the Oak Ridge High School on June 3rd.

Col. Campbell is special assistant for Medical Research to the Commander of Air Force Office of Scientific Research and is one of the foremost authorities in the field of space medicine. The meeting was open to the public and was attended by over 400 persons. The title of Col. Campbell's talk was "Human Parameters of Space Flight." The address was very interesting and answered many of the layman's questions with regard to the aspects of space flight as it affects human behavior, both medically and psychologically.

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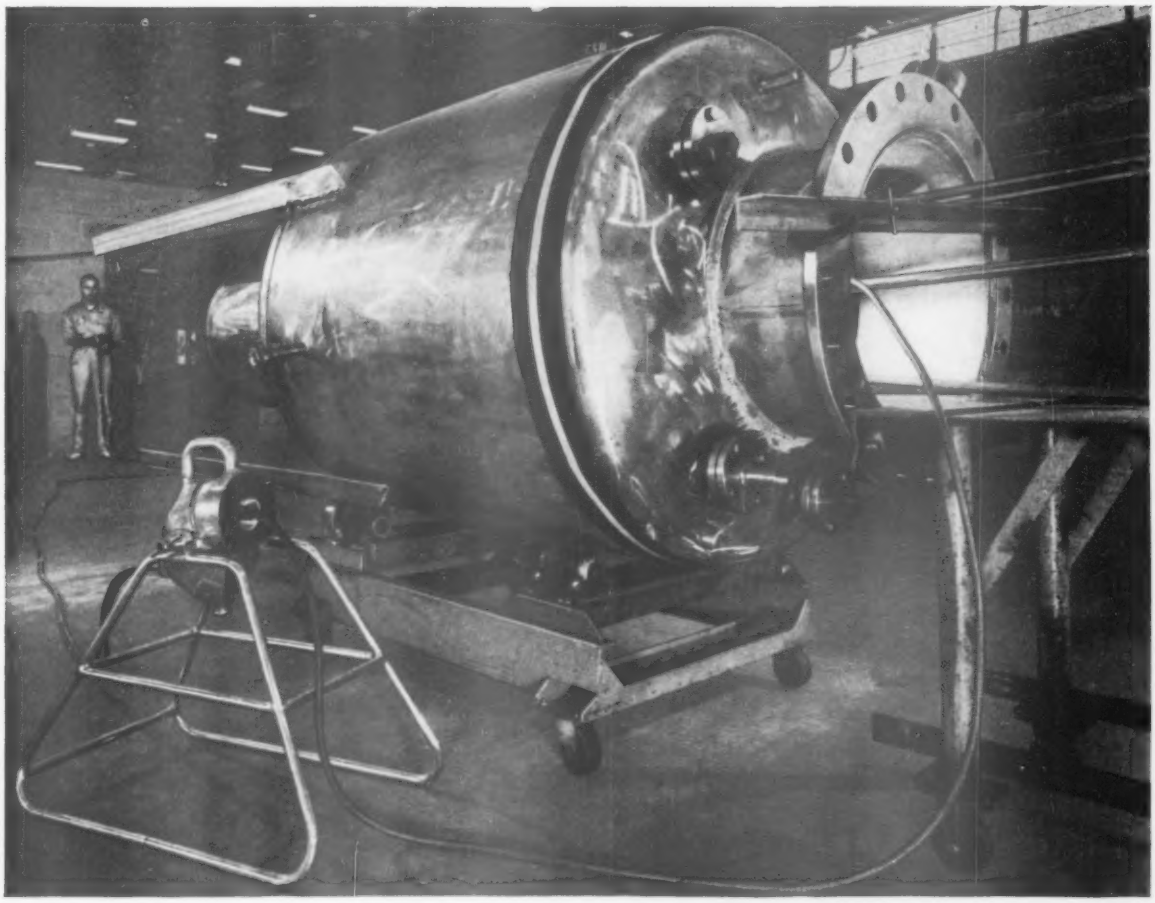

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Shown above is an operator using Nuclear Systems' Model 30, checking a circumferential weld on a fabricated stainless steel pressure vessel, an evaporator used for waste disposal processing of radioactive materials.

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*MEMBERSHIP CLASSIFICATION*

A—Sustaining Member B—Member C—Associate Member
D—Student E—Honorary Member F—Life Member

Total National Membership

| | |
|---|---|
| *Sustaining Members* | 187 |
| *Members* | 6,124 |
| *Associate Members* | 5,846 |
| *Student Members* | 185 |
| *Honorary Members* | 9 |
| *Life Members* | 15 |
| *Total* | **12,366** |

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Smith, Ralph H., Jr. (B)

**ATLANTA**
Snipes, Richard Ambrose (B)
Wells, William S. (B)

**BOSTON**
Chew, Harvey L. (C)
Wells, Richard L. (C)

**BRIDGEPORT**
Fagan, Peter R. (B)
Paul, Norman H. (B)
Weinschenk, Fred

**CAROLINA**
King, Samuel Johnson (B)

**CHICAGO**
Hawse, Everett L. (C)
Johnson, Walter K. (C)
Woelfle, George A. (C)

**CLEVELAND**
Fitzgerald, Edward R. (C)
Golder, Morton H. (B)
Grugel, Carl (C)
Mazurek, Henry P. (C)
Staicer, Anthony (B)

**COLUMBUS**
Davis, E. Glyndon (D)

**DAYTON**
Swisher, Richard G. (C)

**DETROIT**
Elder, William (B)
Kevorkian, John (C)
Larsen, Benth H. (B)
Morrissey, James P. (C)

**FOX VALLEY**
Hart, Edwin L., Jr. (C)

**HARTFORD**
Dobosz, Anthony P. (B)

**HOUSTON**
Elsik, Joe (B)
Gartner, Fred W., Jr. (C)
Kirkland, R. L. (C)
Norwood, Bryan J. (B)
Stephens, Raymond W. (B)

**LEHIGH VALLEY**
Agnew, Stanley Adam (B)

**LONG BEACH**
Champieux, Arthur L. (C)
Twining, Edward M. (B)

**LONG ISLAND**
Gallagher, Joseph A. (C)

**LOS ANGELES**
Burns, Edward S. (B)
Coffin, B. R. (B)
Harrison, J. R. (B)
Nemec, Robert W. (B)
Tryon, Jerry Robert (B)

**MILWAUKEE**
Gould, C. Elmer (B)

**NASHVILLE**
Aschinger, R. R. (B)
Coffee, Robert E. (B)
Ewing, J. O. (B)
Franklin, Harry, Jr. (C)
Frost, Jackson M. (B)
Henshaw, R. A. (C)
Krueger, Harold Frederick (B)
Liddle, Robert W. (B)
Matthews, Elliot (B)
Moseley, Charles (B)
Orning, A. W. (C)
Tune, William W. (C)
Weimar, Edward B. (B)

**NEW HAMPSHIRE**
Nott, Kenneth A. (C)

**NEW YORK**
Ginnane, Robert J. (B)
Lee, Elmer C. (A)
Leight, Lillian (C)

**NORTHWEST**
Reinfeld, W. W. (C)

**PASCAGOULA**
Underwood, John J. (B)

**PHILADELPHIA**
Deery, John F. (B)
Demchick, Herbert A. (C)
June, Carl R. (B)
Karian, George G. (C)
Stark, Donald A. (B)

**PITTSBURGH**
Blaha, Walter J. (B)
Davenport, William J. (B)
Smeltzer, Gerald Eugene (B)

**PROVIDENCE**
Jenkins, Edward J. (B)
Monaco, Vincent (C)

**PUGET SOUND**
Hillistad, Richard G. (B)

**RICHMOND**
Florestano, Ernest J. (C)
Winall, Barney B., III (C)

**SAN FRANCISCO**
Skaggs, Robert L. (B)

**SANGAMON VALLEY**
Fichter, Virgil Charles (B)

**SOUTH FLORIDA**
Byrne, James J. (B)

**STARK CENTRAL**
Saylor, Richard E. (C)

**TOLEDO**
Inlow, Herbert D. (B)

**WASHINGTON, D. C.**
Smith, John Marshall (C)

**WESTERN MICHIGAN**
Schuster, John M. (C)

**MEMBER NOT IN SECTION**
Cosimi, Lamberto (B)

## *Members Reclassified*

During the Month of July

**BIRMINGHAM**
Bates, John W. (C to B)

**DETROIT**
Broniak, Leonard R. (B to A)

**INDIANA**
Roth, Robert Edwin (D to C)

**LOS ANGELES**
Schnabel, Robert L. (D to C)

**MILWAUKEE**
Riteris, Olgert J. (C to B)

**NEW YORK**
Matthews, L. G. (B to A)

**NORTHEAST TENN.**
Morris, L. A. (C to B)

**NORTHERN NEW YORK**
Meister, Robert P. (D to C)

**NORTHWEST**
Ballenbach, W. M. (B to A)

**OLEAN-BRADFORD**
Schneble, F. C. (C to B)

**PITTSBURGH**
Werner, Ephraim (B to A)

**WASHINGTON D. C.**
Letner, R. E. (C to B)

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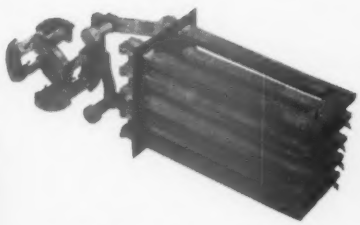
Xyron 2-25 has had widespread successful application in the repair of machine bases, cracked motor blocks, motor and gear housings, transmission cases, gears, cams and building-up of teeth in cast gears. It is extremely valuable in permitting welds without fusion or distortion of the base metal.

Weldors repaired this casting with Xyron 2-25 in 78 hours, including all machining and assembly.

Now, after more than six months of continual operation, no pressure leaks or defects of any kind can be found in the weld. The saving of $5000. and many weeks of non-operation, again points up the tremendous savings that are accomplished in over 100,000 plants by the utilization of Eutectic's "Low Temperature Welding Alloys."®



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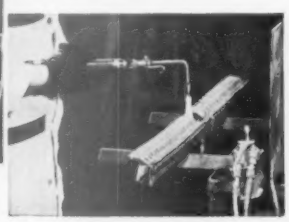
Rocker arms being sprayed with the Spraywelder.



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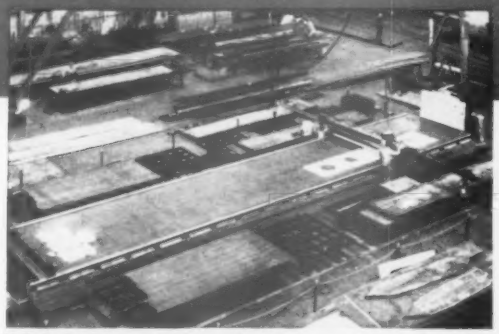

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design throughout Europe and in other countries. Now in America, Messer cutting machine versatility of design permits greater shop flexibility and the increased use of automation that leads to **greater profits.**

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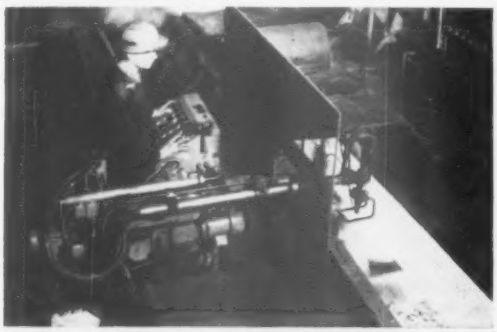

Complete automation is a key feature of Messer cutting machines, as used in this steel mill.

## Machines

# ABSTRACTS OF CURRENT PATENTS

## prepared by Vern L. Oldham

Printed copies of patents may be obtained for 25c from the Commissioner of Patents, Washington, D. C.

2,812,419—Welding Procedure for Joining Aluminum Plates—John J. Chyle, Wauwatosa, Wis., assignor to A. O. Smith Corp., Milwaukee, Wis., a corporation of Wisconsin.

This process for joining the adjacent edges of two high strength aluminum alloy articles uses a multilayer weld for securing the articles together. First, a welding groove having a dam for supporting a first deposit of filler metal is provided and a first layer of filler metal is fused into the articles. Thereafter at least a second layer of filler metal is fused to the articles. Both of the fusing metals include aluminum and specified other alloying ingredients but with the two fusing materials being of different compositions.

2,813,190—Multiple Arc Welding—Charles R. Felmley, Jr., Livingston, N. J., assignor to Air Reduction Co., Inc., New York, N. Y., a corporation of New York.

Felmley's patent is on the deposition of molten metal onto a workpiece simultaneously from a plurality of continuously fed closely spaced consumable wire electrodes. An inert-gas-shielded welding arc is established between each of these wire electrodes and the workpiece with each of the arcs having the edge of its arc plasmas of the adjacent arcs. Current is supplied to the arc in a sense to maintain adjacent arcs in opposite polarities and in an amount sufficient to project molten metal as a spray of fine droplets within the gas shield from each of the wire electrodes to the workpiece. An agent is continuously supplied to at least the arcs of straight polarity effective to produce in such arcs metal transfer characteristics and electrode burn-off characteristics approximating those of the reverse polarity arc.

2,813,192—Electrical Welding Apparatus—Joseph H. Brooks, Peabody, Marcellus N. Gilbert, Jr., Lynnfield, and Charles L. Toomey, Danvers, Mass., assignors to Sylvania Electric Products, Inc., Salem, Mass., a corporation of Massachusetts.

Brooks' and Gilbert's patent relates to apparatus for welding a lead-in wire to a contact on each of a succession of electric lamps and covers specialized electrode positioning means and a brush in cleaning engagement with the periphery of the electrode.

2,813,193—Arc Welding—Harry J. Bichsel, East Aurora, N. Y., assignor to Westinghouse Electric Corp., East Pittsburgh, Pa., a corporation of Pennsylvania.

This patent relates to an arc welding gun for welding with a consumable electrode and it includes a barrel terminating in a gas nozzle. The barrel includes fluid channels for transmitting cooling fluid to and away from the nozzle and for transmitting shielding gas through the nozzle. A guiding tube is provided within the barrel and the gun is characterized by the use of an insulating tube within the barrel around the guiding tube provided therein.

2,813,333—Method of Uniting Metals and Pack for Same—Leonard Pompa, Ardmore, Pa., assignor to Lukens Steel Co., Coatesville, Pa., a corporation of Pennsylvania.

Pompa's patent relates to a method of preparing a pack that comprises forming grooves on the upper face of a carbon-steel base plate adjacent the side edges of the base plate and placing a sheet of alloy steel smaller than the base plate on top of the base plate with the edges of the alloy sheet extending over and being substantially parallel with the grooves on the base plate. The edges of the alloy-steel cladding plate are next welded to the base plate and thereafter the pack is heated and rolled.

2,813,790—Gold-Copper-Indium Brazing Alloy—Walter L. Hack, San Carlos, Calif., assignor, by mesne assignments, to Western Gold & Platinum Co., Belmont, Calif., a corporation of California.

This new brazing alloy is for use under conditions of elevated temperature and high vacuum for application to parts previously brazed by one of the binary alloys of copper and gold. The new alloy is composed entirely of gold, copper and indium. Said alloy comprises about 60 parts gold, approximately 5 parts indium and the remainder copper.

2,813,967—Multiple Point Welding Electrode—William J. Morrissey, Hatfield Township, Pa., assignor to International Resistance Co., Philadelphia, Pa.

This patent is on an electrode for resistance-welding apparatus. The electrode includes the combination of a holder having a longitudinally extending groove in the bottom thereof, and a shoe of electrically-conducting material in the groove. An elongated electrode tip is attached to the bottom of the shoe and flexible conducting straps extend from each end of the shoe to the holder and are attached thereto.

2,814,095—Clad Metal—Leon J. Lieberman, Philadelphia, Pa.

This new patent relates to a composite comprising a pair of spaced generally parallel weldable metallic backing blocks having inner clean surfaces directed toward one another. The composite is made so that it has two parallel rectangular recesses extending into the interior of the composite from the outside at one edge thereof.

2,814,579—Arc-Welding Compositions—Leonidas K. Stringham, Shaker Heights, and Paul E. Jerabek, Euclid, Ohio, assignors to The Lincoln Electric Co., Cleveland, Ohio, a corporation of Ohio.

The new arc welding flux disclosed by such patent is particularly for use in seam welding operations using a continuously fed electrode. The flux includes a killing agent and a number of specified materials in a finely ground, thoroughly mixed state. The particles are bound together when unreacted to form granules having a free-flowing characteristic.

2,814,713—Welding Machine for Wire Fabrics—Russell M. Glantzer, Washington, Pa., assignor, to Tri-State Engineering Co., Washington, Pa., a corporation of Maryland.

Glantzer's patent is on an electric welding machine for use in making wire fabric and where the machine has upper and lower electrodes disposed transversely of the path of travel of wires through the machine. Means are provided to advance longitudinal wires step by step through the machine and other means are provided to form and feed a stay-forming strand crosswise of the longitudinal wires. Other means are provided to weld the associated wire means together and cut the stay wire into lengths as required.

2,814,714—Sheet Material Spot-Welding Attachment—Dorsey B. Thomas, Savannah, Ga.; Floye S. Thomas, heir and guardian of minor children of said Dorsey B. Thomas, deceased, assignor, by direct and mesne assignments, of one-half to D. B. Middleton and one-half to herself.

The present patent relates to an attachment for spot welding lap joint seams of sheet metal and the like. The attachment is used with a portable electric spot welder having a transformer core, a secondary winding supported on the core and having an exposed secondary winding extension projecting exteriorly of the welder. A welding electrode is in communication with the secondary winding. The attachment includes a detachable anode member of electrically conductive sheet material of generally U-shaped outline and including a mounting leg portion having clamping means on the end thereof for removably attaching the anode member to the exposed secondary winding extension of the transformer. Other special means complete the attachment.

# NEWS of The Industry

## Welding Seminar Planned by University of Wisconsin

The new Wisconsin Center Building will be the site on October 16th and 17th for a welding seminar on "Pressure-Pipe and Pressure-Vessel Welding." This particular program in Madison, Wis., will be under the combined auspices of the College of Engineering of the University of Wisconsin Extension Division, the *Madison* Section of the AMERICAN WELDING SOCIETY and the

Wisconsin Center Building

Wisconsin Chapter of the National Certified Pipe Welding Bureau. The opening day of the conference will feature sessions on ASME pressure-vessel and ASA pressure-piping codes. A panel discussion will be held on ASA, ASME and State codes affecting welding of pressure piping. Subjects on the agenda of the second day will include the qualification and testing for welders for pressure-pipe welding and the qualification of welding procedures for pressure piping. The seminar will be brought to a close with another panel discussion on the problem of certification and testing of welders.

The purpose of the two-day seminar is to acquaint engineers, architects, contractors and welding supervisors with the aforementioned codes in respect to the certification of welding operators and the use of welding procedures.

## 5th Midwest Welding Conference Set for January

The 5th Annual Midwest Welding Conference sponsored by Armour Research Foundation of Illinois Institute of Technology and the *Chicago* Section of the AMERICAN WELDING SOCIETY will be held on January 28th and 29th on the Illinois Tech campus.

The conferences, begun in 1955 to bring together welding researchers and users of the processes to discuss current developments in the field, are attended annually by approximately 250 persons interested in the various phases of welding.

The program for the January meeting will include some 12 papers dealing with latest developments in processes and applications.

The conference sessions will be held in the IIT Chemistry Building at 33rd and Dearborn Sts.

Inquiries concerning the conference should be addressed to Harry Schwartzbart, supervisor of welding research, Armour Research Foundation, 10 W. 35th St., Chicago 16, Ill.

## Weck to Speak at ASM Seminar

Richard Weck, professor at Cambridge University in England, will lecture at the forthcoming "Seminar on Residual Stresses" at the American Society for Metal's 40th National Metal Exposition and Congress in Cleveland from October 30th to November 1st. Dr. Weck is director of the British Welding Research Association and chairman of the Commission on Residual Stresses of the International Institute of Welding.

Lecture topics being covered by Dr. Weck will include "Definition and Origin of Residual Stresses," "Measurement of Residual Stresses," "Influence of Residual Stresses on Deformation, Carrying Capacity, Machining Stability and Stability," "Influence of Residual Stresses on Corrosion and Fatigue Strength," Influence of Residual Stresses on Brittle Fracture" and "Methods for Stress Relieving."

## NEMA Group Discusses Electrode Improvements

Color markings for new electrodes in the iron-powder and low-hydrogen iron-powder fields are expected to be released shortly, it was announced at the meeting of the Arc Welding Section of the National Electrical Manufacturers' Association. Held at The Homestead, Hot Springs, Va., on June 5th and 6th, the members heard Chairman Norcross report on his committee's activity with the U. S. Navy. Mr. Norcross said that an effort is being made to create one denominator on military specifications acceptable to every branch of the service. It was also announced that 5000 copies of the *NEMA Arc Welding Manual* would be printed and distributed to colleges and universities. The group also announced a proposed meeting with the AWS Technical Council to present a program whereby greater consideration be given to the deposited weld-metal chemistry in an electrode's selection and specification.

## RWAA Elects O'Grady and Cox

At the Second Annual Meeting of the Resistance Welding Alloy Association in Detroit this June J. A. O'Grady and J. C. Cox AWS were elected president and vice-president, respectively, of RWAA.

Mr. O'Grady, vice-president of the group since its organization in 1956, is president of Weldaloy Products Co., Van Dyke, Mich.

Mr. Cox, president of S-M-S Corp., Detroit, is a member of the executive committee of the AMERICAN WELDING SOCIETY's *Detroit* Section.

## Linde Erects Hydrogen Plant in Linden, N. J.

To meet the growing needs of industry in the metropolitan area for high-purity hydrogen, Linde Co., Division of Union Carbide Corp., is now operating a new plant at Linden, N. J. The majority of the hydrogen will be used in electronics, pharmaceuticals and for food hydrogenation and processing.

Linde designed and built the plant on land leased from General Aniline and Film Corp., who will supply crude hydrogen for processing at the Linden plant.

## St. Louis Televiewers Learn about Welding

Spurred on by the success of an earlier TV appearance of the art of welding in St. Louis area during the AWS Show in that city in April, welding consultant Ned Nyberg AWS arranged a series of four programs, entitled "Welding on the Farm," shown on Channel 4's early morning "Town and Country" program. The four subjects treated were "Oxy-acetylene Welding, Brazing and Cutting," "Electric Welding on the Farm," "Identification of Metals" and "Maintenance and Hard Surfacing on the Farm." Five demonstrations and films were used to describe the various operations. Welding suppliers in the area supplied equipment, operators and

## When there's *no second chance* to correct weld metal performance

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STAINLESS BARE WIRE

Even the critical countdown can't pre-test every vital factor before launching an earth satellite. Weld metal performance at diverse temperatures, various pressures and speeds can be known only through operation. Explorer I—America's first earth satellite—and Explorer III—designed and built at California Institute of Technology's Jet Propulsion Laboratory—both carry welds made with Arcos materials. Arcos CHROMENAR 12 (Type 410 stainless bare wire) was selected for welding certain components. Here's proof again why Arcos Welding Rods and Bare Electrodes are used so widely when results and performance must be predetermined as accurately as possible. ARCOS CORPORATION, 1500 South 50th Street, Philadelphia 43, Pa.

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literature. Viewers responded from distances of more than 100 miles. As a result, Ted Mangner, station farm director, has already invited Mr. Nyberg to present a similar series in the fall.

### Tube Turns Establishes Nuclear Section

Tube Turns, a division of Chemetron Corp., has established a nuclear energy and power plant section of its sales development department. Frank S. Briggs, a 16-year veteran with Tube Turns, has been named manager of the new section. A graduate of Alabama, Mr. Briggs has been associated with sales since 1955.

### Movie on Aluminum Welding

A 16-mm film, entitled "Aluminum Welding. . . Different, Not Difficult," is available without charge for showings to business firms and interested organizations from Reynolds Metals Co., Richmond 18, Va.

Reynolds also is supplying for the movie audiences a 28-page technical brochure of the same title which repeats and supplements the information provided in the 33-min film.

The full-color sound movie shows that aluminum is easy to join by welding, brazing or soldering, although the techniques are different from those used with other metals. Animation is used to help make the technical presentation simple and interesting.

Requests for bookings of the film should be addressed to Reynolds Metals Co., Advertising Distribution Center, Richmond 18, Va. Interested persons may also obtain copies of the technical brochure by writing to the same address.

### Davis Library Adds to Its Nondestructive Testing Collection

Ohio State University's Davis Welding Library has added reduced facsimiles of all articles published in *Nondestructive Testing* magazine, the official publication of the Society for Nondestructive Testing. Articles are reproduced in entirety on a single card and can be read with a reading glass.

### Eutectic Opens Center in Detroit

Eutectic Welding Alloys Corp., Flushing, N. Y., has opened a new warehouse-service center at 416 W. Eight Mile Rd., Detroit 20, Mich. The new center will be known as the Eutectic Welding Alloys Co. of Michigan, Inc. E. Watnik has been appointed to oper-

ate the installation, according to R. D. Wasserman, Eutectic president, and facilities will include stocks of alloys and fluxes, a Eutectic Welding Institute for the training of welders and technical information service.

### AIME to Hold Metallurgical Meeting

The Metallurgical Society of the American Institute of Mining, Metallurgical, and Petroleum Engineers, has announced that its Fall meeting, sponsored by the Institute of Metals Division, will be held in the Carter Hotel, Cleveland, October 27-30 during the Metal Congress.

Among the many meetings will be fourteen sessions on various phases of research in the area of physical metallurgy. Other sessions will be symposiums developed by technical committees of the Society's three Divisions, the Institute of Metals Division, the Extractive Metallurgy Division and the Iron and Steel Division.

### Los Angeles March Site for Western Metal Congress

The Eleventh Western Metal Exposition and Congress will be held during the week of March 16th at the Ambassador Hotel and the Pan-Pacific Auditorium in Los Angeles, Calif.

Among the 20 societies to participate in the technical programs will be the American Society for Metals, the *Los Angeles* Section of the American Welding Society and the Society for Nondestructive Testing.

### Thermacote Manufactures Abrasive Products

The Thermacote Co. of Pasadena, Calif., announces the availability to welding distributors of a complete line of grinding wheels and abrasive products. The West Coast firm will manufacture these products under the name "Thermo-Cut" and will warehouse them at their facilities in Pasadena, Chicago and Atglen, Pa.

### Midwest Carbide Elects Officers

Midwest Carbide Corp., a Keokuk, Iowa firm, jointly owned by Chemetron Corp. and Shawinigan Products Corp., has named L. F. Loutrel and James W. Dunham as chairman of the board and president, respectively. A producer of calcium carbide, Midwest Carbide operates plants in Keokuk and Pryor, Okla.


## When low alloy weld requirements are as *critical* as these

Job report courtesy of McKiernan-Terry Corp., Dover, N. J.

WELD WITH ARCOS

### LOW HYDROGEN ELECTRODES

This crosshead weldment—part of a Navy steam catapult for launching jet fighters—must withstand the repeated powerful surges of steam under high pressure. Arcos Tensilend 80 electrodes were used to weld the SAE 4130 low alloy steel. After progressive magnaflux checking, and proper stress relieving, all welds were found to meet the high strength and toughness required for this kind of service. For the *right* weld metal—for the *right* welding techniques—for your tough welding problems—call on Arcos. ARCOS CORPORATION, 1500 South 50th Street, Philadelphia 43, Pa.

For details, circle No. 20 on Reader Information Card

## Budd Opens Western Sales Office

The Budd Co. has announced the establishment of a Western Sales Office, with headquarters in Los Angeles, to coordinate West Coast sales activities for two of its subsidiaries, Continental-Diamond Fibre Corp. and Tatnall Measuring Systems Co., and for its Defense and Nuclear Systems Divisions.

Edwin R. Wisner, sales manager of the Defense Division, has been appointed director of the new department which began its activities on June 2nd.

The Nuclear Systems Division produces radiography machines for nondestructive testing, high-strength radioisotopes and radiation facilities for research.

## Gregory Industries Report Record Sales

Gregory Industries, Inc., manufacturers of Nelson stud-welding equipment, report a record sales of $6,126,057 for the fiscal year ending on Apr. 30, 1958. Net earnings were at $314,324, slightly less than the all-time high of the previous year.

President George E. Gregory commented in his annual report that "approximately 26% of our stud welding sales volume was represented in products or applications which did not exist four years ago."

## NCG Builds Oxygen Plant in Erie

An agreement has been concluded between Erie Forge & Steel Corp. and the National Cylinder Gas Division of Chemetron Corp. in which NCG will construct the first industrial gas-producing plant in Erie on leased premises of Erie Forge & Steel.

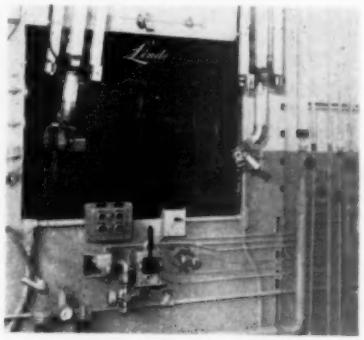
The new plant is basically for the purpose of supplying oxygen by pipe line to meet the steel-making requirements of Erie Forge & Steel Corp.

Oxygen from the new plant will also be supplied to other industries and to hospitals in the Erie area, and acetylene and other gases, welding supplies and medical equipment will be available at the new plant.

Initial capacity will be approximately seven and a half-million cu ft of gas per month. The new plant is expected to be ready for operation by the first of the year.

## Linde Research at New Radiation Lab

To study the effects of radiation on various industrial materials, Linde Co., Division of Union Carbide Corp., has constructed a radiation laboratory with a Cobalt 60 gamma ray source. The "gamma ray" is an exceptionally high energy X-ray that could open the door to an entirely new field of metal treatment. These rays are already being used in biological studies, in X-ray therapy and to determine weld quality by radiography.




The new addition to Linde's other radiation facilities is located on the grounds of Linde's Tonawanda, N. Y. laboratory. The researchers will be protected by the most modern safety equipment, including a 36-in. thick lead-glass observation window and a set of manipulators called "master slaves." The "master slaves" are actually mechanical arms within the cell that react sensitively to controls operated from the other side of the observation window. Using these, operators can move objects inside the radiation cell with no danger to themselves.

A team of Oak Ridge trained scientists will participate in this new research effort.

## BOX CAR TRANSPORTS LIQUID OXYGEN

Linde Co., Division of Union Carbide Corp., makes use of this box tank car to transport liquid oxygen, nitrogen and argon. The cars have an oxygen capacity of the liquid equivalent of one million cu ft NTP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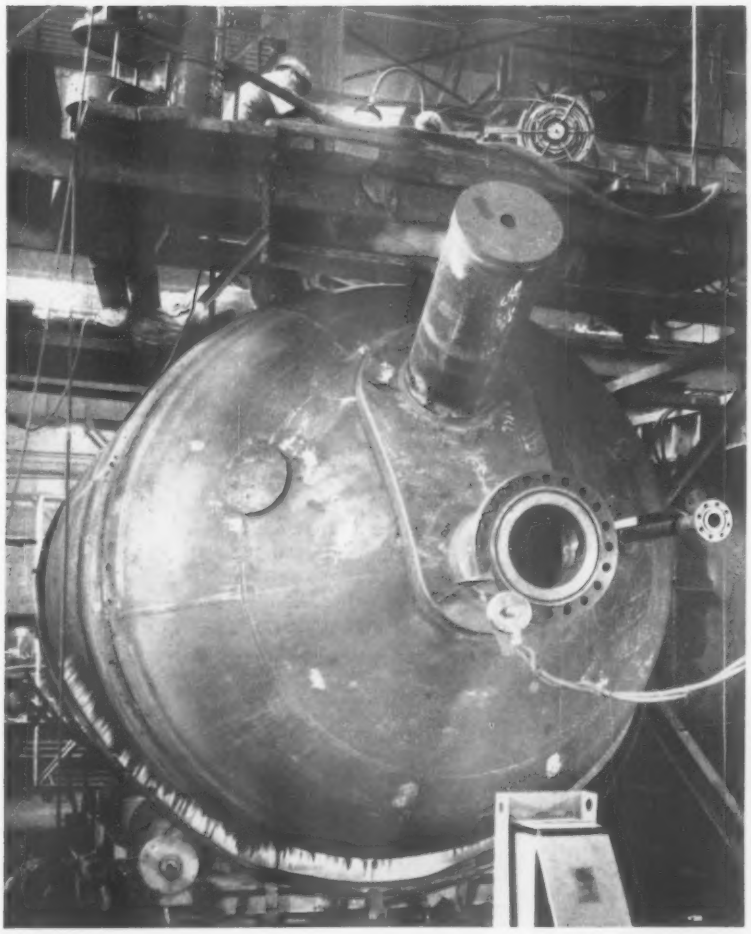

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# INTERNATIONAL WELDING NEWS

By Gerard E. Claussen

## Soviet Advances

### *Electro-Slag Welds*

The fatigue strength of electro-slag welds in plates 10 and 12 in. thick has been determined[1] on specimens from 3/4 to 8 in. diam and also 8 in. square. The 12-in. plates were spaced 1 1/4 in. apart and were welded with three 1/8-in. electrodes extending 3 in. beyond the contacts using 425–450 amp, 48–50 v. The electrodes moved horizontally 26 ipm and hesitated 5 to 6 sec at each wall. Wire feed was 110 ipm, while travel speed upward was 1/4 ipm. The weld metal contained 0.13 to 0.22% C, 0.68 to 1.10% Mn and 0.04 to 0.17% Si. Specimens 3/4 in. in diameter cut from the weld had a fatigue strength of 20,600 to 24,800 psi compared with 26,200 to 30,600 psi for the plate which contained 0.19 to 0.31% C, 0.65 to 1.08% Mn and 0.14 to 0.37% Si. Normalizing or normalizing and drawing had little effect on the 3/8-in. specimens. The results on 8-in. square specimens were 20,600 psi for the plate, 19,200 psi for the weld without heat treatment, 17,800 psi after normalizing at 1720° F and drawing at 1150° F, and 14,900 psi after normalizing only. The fatigue values were based on 10 million cycles. For specimens 8 in. in diameter normalizing produced higher fatigue strength (20,900 psi) than as-welded (19,200 psi). It was concluded that for machined welds heat treatment is not necessary to secure high fatigue strength. The 25% difference in fatigue strength between small and large specimens was not explained. Slag inclusions lowered the fatigue strength and are the subject of a study now in progress.

**Dr. Gerard E. Claussen** is associated with the Development Laboratories, Linde Co., Newark, N. J.

### *Heat Flow in Submerged-Arc Welding*

A method of calculating the flow of heat from the arc crater in submerged-arc welding is presented by Petrunichev.[2] The heat flow was found to be less concentrated as the current or voltage was increased and as the travel speed was decreased. The formula is based on the Gauss heat-flow equation and is in close agreement with the thermocouple surveys of the plate adjacent to the weld during welding at 550 to 1200 amp.

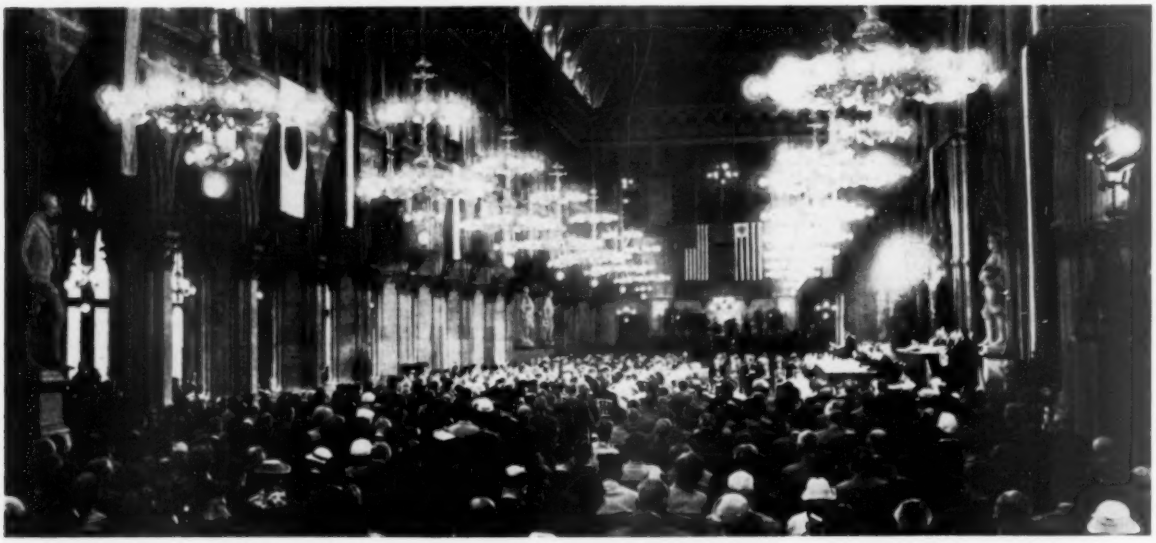
### *Direction of Carbon Migration Predicted*

Two Russian authors[3] described a method of predicting the direction of migration of carbon when a welded joint in low-alloy steel is heated for long periods at 1300° F. The method is based on converting the atomic percentages of carbon and alloying elements to carbides. There will be a surplus or deficit of carbon depending on whether there are insufficient or excess alloying elements to form carbides. If the base metal has a surplus of carbon and the weld metal a deficit, for example, migration will occur from the base metal to the weld. The method was confirmed experimentally. It was shown that the abrupt change in carbon content at the fusion line after 100 hr at 1300° F can be equalized by heating at 1750° F.

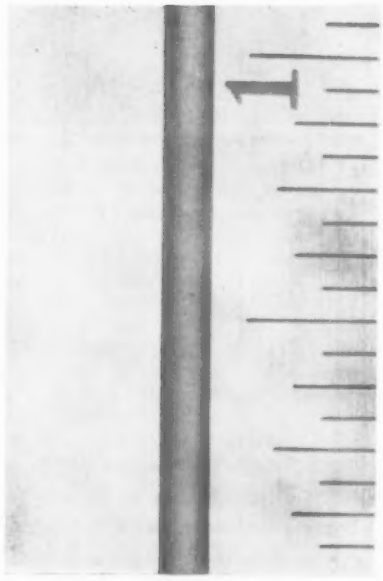
### *Low-Hydrogen Weld-Metal Sampling Method*

A novel way of sampling weld metal was used by Kozlov[4] in a study of hydrogen in weld metal during welding with covered electrodes and by the submerged-arc process. A 5/16-in. hole was drilled in the plate in the path of the submerged arc. The weld metal flow-

## WORLD LEADERS OF WELDING ATTEND OPENING SESSION OF IIW IN VIENNA

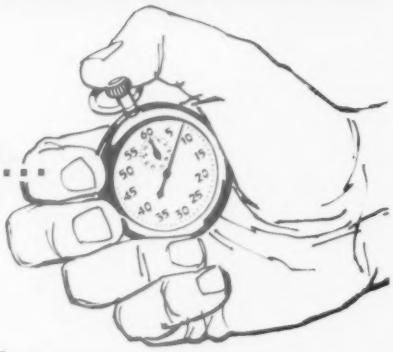


East and West sit side by side during the formal opening session of the International Institute of Welding in Vienna, Austria; on June 29th. The delegates and guests heard addresses by the president of the Austrian Welding Society, Hans Lauda, IIW President Ugo Guerrera of Italy; Mayor Franz Jonas of Vienna; the Federal Minister of Trade & Reconstruction, Fritz Bock; and last but not least, the President of Austria, Adolf Scharf. (More complete coverage of this meeting will appear in the October issue of the JOURNAL)



## Measure the build-up height...

## Clock the deposition rate...



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Unlike most build-up materials, *Stoody Iron Powder Build-Up does not depend upon carbon to develop hardness!* Its high alloy content produces a more uniform Rockwell hardness despite wide variations in base metal analysis.

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ing through the hole was caught in a copper mold and solidified as a $^3/_8$-in. cylinder. With covered electrodes, the electrode was deposited in a groove in a water-cooled copper plate. The hydrogen content was measured by evolution at room temperature and by vacuum extraction at 1100 to 1300° F. Curves were drawn relating hydrogen solution at room temperature to drying temperature and to exposure time at two humidities after drying for a covered electrode. The drying temperature had to be raised to 800° F to lower the hydrogen content below 1.0 cu cm per 100 g. Moisture pickup after 10 days raised the hydrogen content to 1.5 and 3.5 cu cm per 100 g, respectively, for electrodes dried at 860 and 716° F and exposed to 45–55% humidity at room temperature. A long arc resulted in lower hydrogen content than a short arc. Likewise, in submerged-arc welding high voltage lowered the hydrogen content compared with low voltage.

#### *Deformation Measurements in Steel Cylinders*

Measurements of deformation and residual stresses in automatic arc welding and resistance seam and spot welding sheet steel cylinders were made by Russian writers.[5] Rolls for correcting the deformation were described.

### Polish Welding Conference Held

A welding conference[6] was held in Poland Oct. 24–26, 1957, which was attended by 250 delegates from Russia, East Germany, Czechoslovakia, Hungary and Yugoslavia. The conference was organized by the metallurgical committee of the Polish Academy of Science and by the welding section of the Union of Polish Engineers. The following papers were delivered:

1. Properties of a Low-Hydrogen Electrode (Welding Institute of Gliwitz). The impact properties of the weld metal were insensitive to the moisture content of the coating.

2. Use of Arc Welding for Drop Forging Dies (Hungary).

3. Welding Repairs in Metallurgical Plants (East German Welding Institute).

4. Reconditioning Rolls by Submerged Arc Welding (Gliwitz).

5. New Information of Welding Materials (Welding Institute of Bratislava).

6. Russian Electrodes for Arc Welding and Surfacing (Moscow).

7. Welded Bridge at Belgrade (Hungary). The bridge was nearly 900 ft long and some low-alloy steel was used in its construction. Impact requirements were met at +32° F.

8. Prevention and Removal of Welding Distortion in Ship Construction (Leningrad Shipbuilding Institute).

9. Effect of Defects on the Static Strength of Welds (Warsaw Shipbuilding Institute). Provided lack of fusion did not exceed 7% of the cross section; static strength was unaffected.

10. Overload Capacity of a Combined Welded and Riveted Bridge (Gliwitz).

#### *Exhibit Featured Automatic Welders and Electrodes*

There was a large exhibit of Polish welding equipment of all kinds, including automatic welders of Russian design, and 15 types of electrodes for structural steel. The latter were of iron oxide, rutile and low-hydrogen grades. Visits were paid to several plants including Guta Beldon plant in Silesia where all Polish electrode manufacture is concentrated. The Guta Ferrum Boiler works in Catowitz welds drums up to 4 in. thick but uses no alloy steel. The visitors noted a widespread use of automatic welding and air-carbon arc cutting in Poland.

### Brazing Materials Tested in France

Short-time and creep tests in shear were made by Blanchet[7] on five brazing materials in three high-temperature materials: 0.8% Cr steel, Type 321 stainless steel and 75Ni – 20Cr alloy. The brazing materials were: 50% Ag, 16% Cu, 16% Zn, 18% Cd (1161–1175° F), 72% Ag, 28% Cu (1436° F); 85% Ag, 15% Mn (1767–1778° F). The freezing interval is given in parentheses. The joint thickness was 0.0024 in. for the silver-base alloys and 0.0004 in. for the phosphor-bronze alloys. The limiting temperature for good short-time and creep strength for each brazing material was 500, 707, 734, 1157 and 1526° F, respectively. The bronze caused intercrystalline brittleness in austenitic steels, while the Ag-Pd alloy required too high a brazing temperature.

### Austrians Discuss Welding of Plastics

The February issue[8] of the Austrian welding magazine is devoted to the welding of plastics. One article lists the composition of various weldable plastics while another illustrates several welding processes. The third article is a reprint of a 1955 paper. The Swiss Welding Society issued a brief humorous brochure on polyvinyl chloride plastics welding. The brochure is interweaved with their annual report and is illustrated in color.

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10. "Schweissen von Gusseisen." Vol. 4 of *Fachbuchreihre Schweisstechnik*, published by Fried. Vieweg & Sohn, Braunschweig, Germany, 107 pp. (1958).



## *Seminar on*
# RESIDUAL STRESSES

- **What Are They?**
- **How Do They Get There?**
- **Are They Dangerous?**
- **How Can You Get Rid of Them?**

Prof. R. Weck of Cambridge University and Director of the British Welding Research Association, will be brought to America this October to deliver seven important lectures on residual stresses. Coupled with these outstanding lectures will be seven discussion periods by 14 American experts.

This Seminar will be held during the latter part of Metal Show Week

**October 30, 31 and November 1, 1958**

**Place:** Wade Park Manor, Cleveland

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# PERSONNEL

## Finlay Heads Crucible Research Group

W. L. Finlay (MS), former manager of Midland Research Laboratory, has been promoted to director of research of Crucible Steel Co. of America's newly formed Technology Department. Dr. Finlay will now be responsible for steel, titanium and special alloys research.

A chemical engineering graduate from Lehigh, Dr. Finlay earned his Master's and Doctor's degrees in Metallurgical Engineering from Yale. He is also a member of Phi Beta Kappa and Tau Beta Pi.

## King Appointed Sales Manager at Square D

Robert E. King has been named distribution equipment sales manager for Square D Co., Detroit, Mich. Mr. King has been sales supervisor at the

Robert E. King

Detroit headquarters since 1953. He has been associated with Square D since his graduation from Purdue in 1947.

## Wedereit Heads Chemetron Advertising

Gene Wedereit has been promoted to the corporate staff as director of advertising and public relations for Chemetron Corp.

Mr. Wedereit has been with the company 15 years, serving previously as director of advertising for several of the company's divisions.

Mr. Wedereit has served as president of the National Industrial Advertisers Association and as chairman of the board of the Industrial Advertising Research Institute.

## Tolliver Promoted by Tube Turns

Jack D. Tolliver has been elevated to the new position of sales manager of the Eastern region by Tube Turns, a division of Chemetron Corp. He will be succeeded as manager of the sales development department by Edward F. Harrington.

A University of Louisville graduate, Mr. Tolliver will now supervise sales in the Eastern region, namely: New York, Philadelphia, Pittsburgh, Chicago, Louisville and Atlanta.

Mr. Harrington, an alumnus of Northeastern University, has been associated with Tube Turns since 1954.

## Pond Named Personnel Head at Chemetron

Samuel A. Pond has been appointed to the newly created post of personnel director on the corporate staff of Chemetron Corp.

Mr. Pond graduated from the Sheffield Scientific School of Yale University in the class of 1936 and obtained his MBA degree in 1939 at the Graduate School of Business, Stanford University.

## Teng Joins Meissner Engineers

John F. Meissner Engineers, Inc., Chicago, announce that Wayne Teng has been appointed head of their Structural Department. Dr. Teng studied under General Chiang-Kai-shek when the latter was on the faculty of the National Central University of China in Chungking. Previously Dr. Teng had been associated with Skidmore, Owings & Merrill and had served on the faculty of Illinois Institute of Technology.

## Giler Named by Westinghouse

Roger R. Giler has joined the welding division of Westinghouse Electric Corp. as product planning specialist. Mr. Giler is a graduate of the University of Strasbourg and also holds a degree in metallurgical engineering from Carnegie Tech. He originally joined Westinghouse in 1953 as a member of the industrial heating division.

## OBITUARY

## William Hunt Eisenman

Nationally and internationally known among members of the metallurgical profession and in the metalworking industry, William Hunt Eisenman, 73, secretary and a founder-member of the American Society for Metals, died Friday, May 30th in a LaJolla, Calif. hospital.

William H. Eisenman

Mr. Eisenman, who served the American Society for Metals for 40 years and was managing director of the National Metal Exposition and Congress (Metal Show), succumbed following a heart attack at his Palm Springs, Calif. vacation home.

A native of Jamestown, Ohio, Mr. Eisenman joined the American Society for Steel Treating, forerunner of the ASM, in 1918 as national secretary. Under his leadership the society grew from a one-desk organization with fewer than 200 members to more than 30,000 members in 105 Chapters in the U. S. and Canada.

The late ASM secretary was educated at Kenyon College, Stanford University, Morningside College, Iowa and Ohio State University. After a brief period as a professional football player with the old Olympic Club, he began his career as a high school principal in Olathe, Kan., was head of the chemistry department at Racine, (Wis.) College, and superintendent of schools in Elmhurst, Ill.

His thousands of friendships in the metalworking industry were made during his long service as managing director of the National Metal Show, one of the nation's largest industrial expositions.

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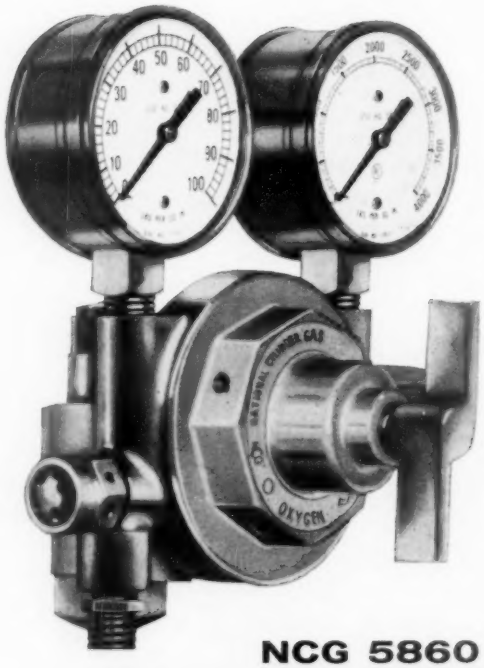


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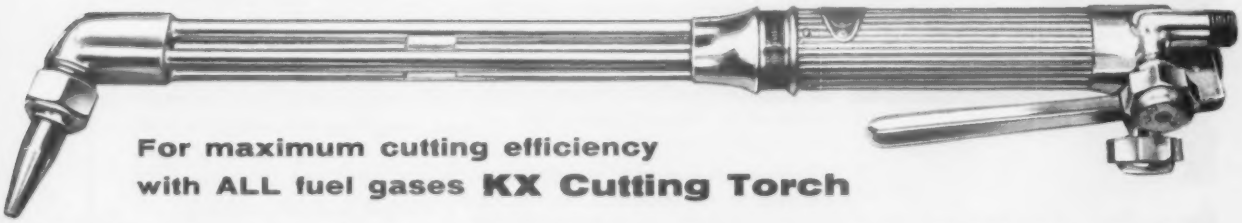

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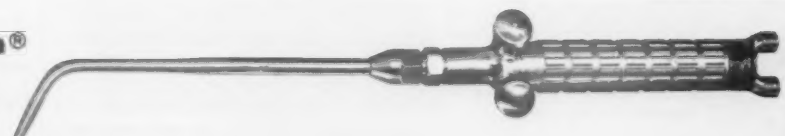
## NCG 44 Bantam® Welding Torch for LIGHT welding



The one light welding torch with all the features welders look for. Welds aluminum and alloys, aircraft tubing and steel from 28 gauge to 3/8" thick. It's light, easy handling, compact—*gets into an absolute minimum of space.* Valve wheels are right under your thumb for quick adjustment. Stainless steel stems are non-seizing and non-scoring. Nickel alloy tubes are silver brazed for added strength. Uses swaged-style Series NCG-71 Tips or screw-in style Series NCG-170 Tips. Length, less tip—6½".

*Registered trademark of The Bastian-Blessing Co.

For details, circle No. 24 on Reader Information Card

SURVEY
RESULT #2*

98%

of Welding Journal readers find the Journal CONTENTS HELPFUL

In our Message No. 1, we told you that the Journal enjoyed 98% **readership.** Tie that in with a similar percentage who find the contents HELPFUL and you have further proof that WJ contents are **basic** to the whole welding profession. And with good reason: The material for our book is written by top welding people, double-checked for accuracy by experts in the field, and exposed to the analytical eyes of thousands of Society Members — also tops in the Welding Industry. No wonder the Journal is welcomed here and abroad as the foremost source of **accurate** welding reporting and information. If you have a logical product to sell in the welding field, your most logical bet is the Welding Journal.

* At the request of a leading advertising agency, a survey was directed to a group of Welding Journal readers specified by the agency. Here is one of the many interesting points that came to light. The actual surveys are available for inspection at any time.

**WELDING JOURNAL**

Besides the National Metal Show, Mr. Eisenman also directed the Western Metal Exposition and Congress biennially at Los Angeles, for 20 years, and shortly before his death had launched the new Southwestern Metal Exposition and Congress, at Dallas, Tex.

His contacts were world-wide and he was originator and secretary general of the first World Metallurgical Congress at Detroit in 1951 and of the second Congress at Chicago in 1957. Both drew metals scientists from 40 countries.

Mr. Eisenman was honorary member of Die Deutsche Gesellschaft für Metalkunde (Germany), Société Francaise de Metallurgie (France), Association des Ingenierures Sortis de l'Ecole de Liege (Belgium), and the Iron and Steel Institute (Great Britain). He was awarded the A.S.M.'s Gold Medal in 1956, for distinguished contribution to metallurgy.

During World War II he served as secretary of a special committee of 20 scientists working under auspices of the National Academy of Sciences on wartime metals problems and metals conservation.

He had been secretary of the Metal Treating Institute, president of the National Association of Exhibit Managers and the American Convention Institute. He was on the advisory committee for the Chicago World's Fair (1933) and director of St. Louis, Philadelphia and Kansas City civic celebrations.

During the past year, Mr. Eisenman donated to the American Society for Metals a 100-acre tract on his Russell Township farm, "Sunnimoor," in Geauga County east of Cleveland, Ohio. Construction is now underway there on the "ASM of Tomorrow," new $2,000,000 headquarters building for the society. Feature of the building will be a huge geodesic dome, visualized by Mr. Eisenman as symbolic of the world's metallurgical progress.

At the time of his death, Mr. Eisenman was scheduled to receive an honorary Doctor of Laws degree from Western Reserve University, Cleveland, Ohio.

For some 25 years, the AMERICAN WELDING SOCIETY participated in the "Metal Show" under the sponsorship of the American Society for Metals. This association was terminated in the fall of 1956. During the years when the SOCIETY participated in the "Metal Show," Mr. Eisenman provided the over-all leadership needed for such an undertaking. His friends and contacts within the SOCIETY were many.

**WELDING ENGINEERS**

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Los Angeles Division of NAA is looking for graduate engineers with at least two years experience in welding to work on hot, high-flying weapon systems and space ships such as B-70, F-108, and X-15.

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# EMPLOYMENT SERVICE BULLETIN

## Positions Vacant

Welding Engineer. A challenging opportunity with large engineering and construction firm for a graduate engineer with a minimum of three years' experience preferably in shop fabrication of field construction on pressure vessels, power and refinery piping. The Company designs and constructs steam, electric, hydro and nuclear power installations, refinery and chemical plants and large pipe lines. Qualifying candidates must have working knowledge of manual arc, heliarc, submerged arc, and processes for code welding. Work involves developing welding procedures, preparing specifications, and advising design engineering and construction personnel. Job location in San Francisco and relocation expenses will be paid. Some traveling will be required. Send outline of personal history, experience and salary requirements to: George I. Copeland, Manager of Employment and Placement, Bechtel Corp., 150 Sansome St., San Francisco, Calif.

Sales representatives desired for Eastern Central, Midwestern and far western states by leading AWS and RWMA member manufacturing complete line welding machinery, transformers and automation equipment. Applicants must be thoroughly familiar with welding practices and techniques. Will consider established manufacturers' representatives or sales agents carrying noncompeting lines. Submit resume of business and personal history to Kirkhof Manufacturing Corp., 2450 Buchanan, Grand Rapids, Mich.

## Services Available

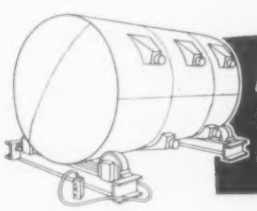
A-706. Welding Supervisor. Craft Coordinator. Twenty-nine years in welding and related activities. Last 17 in supervision, inspection and coordination of crafts. Twelve years overseas—major companies. Heavy code experience; hi-temp. and pressure, high and low-alloy piping and vessels. Many processes. High and trade school graduate. Some formal engineering and metallurgical education. Knowledge of Spanish. Prefer position in heavy industry, nuclear or construction field. Can assume heavy responsibility. Family man; good habits. Will relocate (domestic or abroad) or travel.

A-708. Engineer. Experienced in welding, electrical and electronics fields. Bachelor's degree in Electrical Engineering with some graduate work. Employed almost 5 years by well-known manufacturer of resistance-welding equipment in final shop setup and testing, including butt, spot and seam welding of steel, steel alloys and aluminum. Complete qualification of machines to Military Specifications. Operation and development of X-rays, use of welding and electronic laboratory equipment. Traveled past three years as service engineer. Prefer location in Florida or warm climate.

A-709. Staff Welding Engineer. Married. Age 30. Veteran. Seven years mechanical and welding engineering. Past five years welding engineering supervisor for large punch press corporation. Experienced in light and heavy fabrication; trouble shooting; standards, code pressure vessels; vendor and customer relations.

A-710. Brazing and Soldering specialist. Development and manufacture of fluxes, alloys. Methods. Available consultant or part time position.

**ATTEND**
**AWS**
**40th ANNUAL MEETING**
**April 6–10, 1959**
**Chicago, Ill.**

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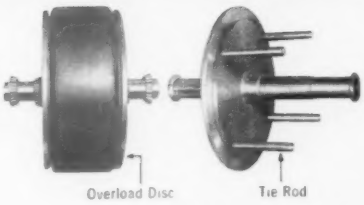


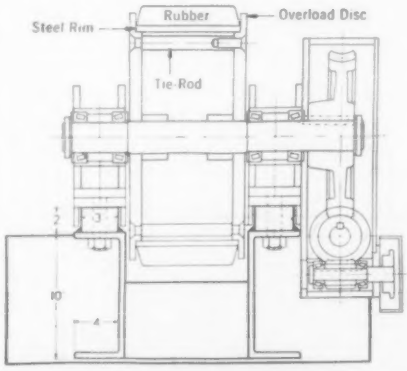


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For details, circle No. 25 on Reader Information Card

# NEW LITERATURE

## Hard-Surfacing Catalog

A new hard-surfacing electrode catalog, Bulletin No. MW-222, announced by the Welding Products Division, A. O. Smith Corp., Milwaukee, Wis., outlines data on more than 25 different electrodes and wires. Of particular interest is a "checklist of uses"—a guide for selecting the correct electrode or wire to use on more than 400 different types of welding equipment.

For your free copy, circle No. 26 on Reader Information Card.

## Machine Design

The Lincoln Electric Co., Cleveland 17, Ohio, has issued 4-page Machine Design Sheet No. 1202.19D which describes the method used by the Providence, R. I. firm of Brown & Sharpe Mfg. Co. to reduce manufacturing costs of a fabricated base for a grinder. This particular machine design resulted in the replacement of the former cast-iron base with one of welded steel.

For your free copy, circle No. 27 on Reader Information Card.

## Description of ASM Meeting

American Society for Metals, 7301 Euclid Avenue, Cleveland, Ohio, offers an illustrated folder which gives complete details of sessions on "Residual Stresses" to be presented at Metallurgical Seminar in Cleveland this fall.

For your free copy, circle No. 28 on Reader Information Card.

## Inert-Gas Welding

Linde Co., Division of Union Carbide Corp., 30 E. 42nd St., New York 17, N. Y., has released its July 1958 issue of the "Linde Metalworking Bulletin." This 12-page booklet, in color, contains five articles on various applications of arc welding. Two treat the use of inert-gas-shielded tungsten-arc welding in the automotive and pleasure-boat industries, while another article describes an aluminum-bronze surfacing operation in which inert-gas-shielded metal-arc welding is used. Other editorials in Vol. 9, No. 4 of the 8½ x 11-in. publication deal with welding's role in the fabrication of Naval bombs and the fabrication of large trucks.

For your free copy, circle No. 29 on Reader Information Card.

## Data Charts on Bronze Electrodes

Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis., offers a series of data charts on comparative bronze electrodes, melting rates and efficiencies, electrode and filler rod specifications and recommended welding currents.

Ampco's welding rod color code, deposit density, number of electrodes per lb and ft per lb of bare rod are also detailed.

For your free copy, circle No. 30 on Reader Information Card.

## Catalog on Welding Cable

Belden Manufacturing Co., Chicago 80, Ill., has released through its welding cable distributors a new catalog which contains illustrations and descriptions of their line of welding cables.

Belden's line of welding cables includes both the new neoprene-jacketed and the Belden OTR-jacketed cables in sizes 4/0 through 6; the new neoprene jacketed extra limp whip leads in sizes 1/0 through 2; welder replacement cords; and 2, 3 and 4 conductor power supply cords in sizes 6 through 14.

For your free copy, circle No. 31 on Reader Information Card.

## Bench-Mounted Spot Welders

A new 8-page bulletin, No. 2-013A, from the Taylor-Winfield Corp., Warren, Ohio, describes the company's line of EB-1 air- or foot-operated bench-mounted spot welders. The units are specifically designed for joining small parts of unusual metal combinations of precise shapes and sizes, particularly delicate electronic components, fine jewelry, scientific instruments and similar products. Air-operated EB-1 welding heads may also be incorporated with dial feed tables or other equipment.

The welder proper consists of a cast main frame mounted on a tubular column which also supports the lower welding horn. The column is supported by the welder base on the bench top. Construction permits vertical adjustment of the lower horn for most convenient work height and adjustment of the opening between the lower horn and upper electrode for adequate work clearance.

For your free copy, circle No. 32 on Reader Information Card.

## Wear-Resistant Alloys

Haynes Stellite Co., Division of Union Carbide Corp., Kokomo, Ind., has published the July 1958 issue of the "Haynes Alloys Digest." Volume 9, No. 7 of this 4-page, 8½ x 11-in. folder contains four feature articles with photographs including "Hard Alloy Inserts Resist Wear: Mean Fewer Adjustments to Clutch" and "Heat Treat Tray in Service Again After Three Years at 1450–2040° F."

For your free copy, circle No. 33 on Reader Information Card.

## Tungsten-Carbide Powder

An 8½ x 11-in. illustrated 2-color engineering data sheet, No. 21, describing Colmonoy No. 75, a new tungsten-carbide "Sprayweld" powder, is now available from Wall Colmonoy Corp., 19345 John R. Street, Detroit 3, Mich.

The new data sheet describes the content, properties and typical applications for the new hard-surfacing material. Base metal and overlay thickness recommendations are also included.

Engineering Data Sheet No. 21 also describes the Colmonoy "Spray-weld Process" and gives recommendations for using Colmonoy No. 75 powder with the process.

For your free copy, circle No. 34 on Reader Information Card.

## Flux Information

Eutectic Welding Alloys Corp., 40-40 172nd St., Flushing 58, N. Y., has issued technical publication No. TIS 2839, the "Complete Flux Issue." The 20-page booklet explains the reasons fluxes are used and the advantages of using fluxes with chemical analyses compatible with specific filler alloys.

For your free copy, circle No. 35 on Reader Information Card.

## Production Through Resistance Welding

Issue No. 582 of "Weld-It," published by The Taylor-Winfield Corp., Warren, Ohio, features an illustrated article entitled "Metal Gathering Machines Automate Production at Ford Motor Co.". Other articles included in the 4-page folder are "Resistance Metal Gathering" and "Turbine Blades by Metal Gathering."

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For details, circle No. 37 on Reader Information Card→

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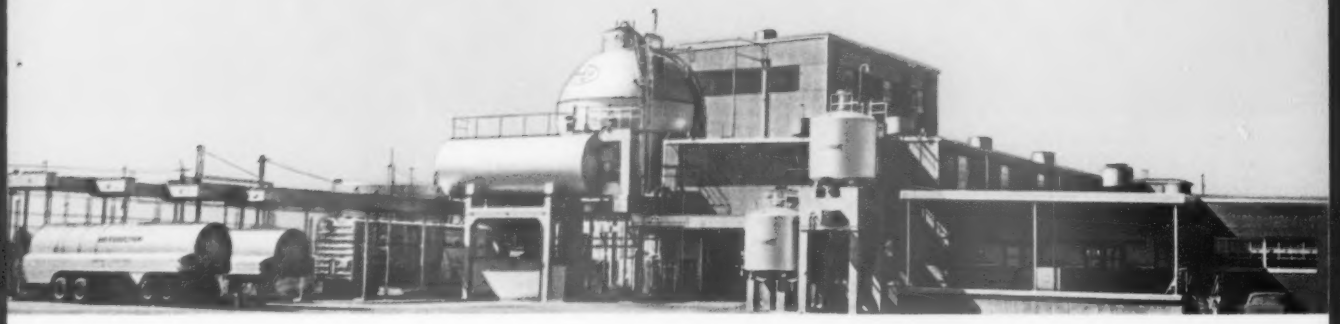

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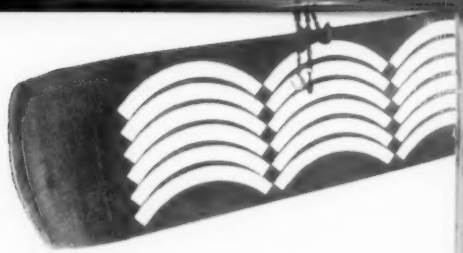

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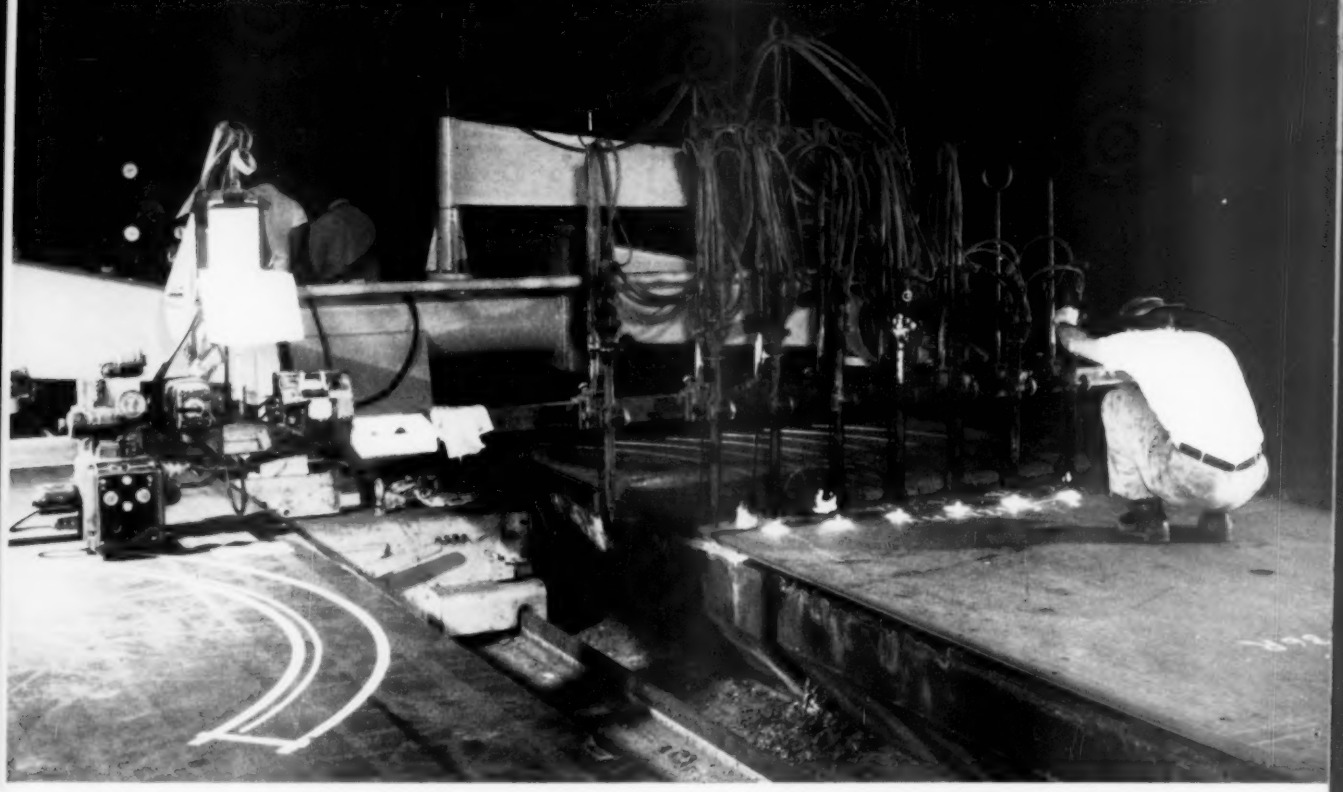

Whether it is the warehousing of steel parts for supply to metal-fabricators, or the production of parts by fabricators for assemblies and end products, multiple-torch flame cutting today plays a role of growing importance in modern industry.

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## Disk Supports for Grinders

The Nedco Co., Waltham, Mass., offers a 2-page data sheet describing the firm's new disk supports for use on grinding equipment. These "cushion type supports" for depressed center type resinoid disks and wheels are available in 3- and 4-in. sizes. According to Nedco, the supports absorb and deaden vibration.

For your free copy, circle No. 38 on Reader Information Card.

## Flame-Cutting Unit

A new 8-page booklet, Form ADC 673, detailing the features of Airco's No. 20 radiagraph flame-cutting machine is now available.

The No. 20 Radiagraph is a portable, motor-driven, straight-line track-guided machine. Limited contour, circular and curved cutting can be accomplished by using suitable accessories, which are also described in the booklet.

For your free copy, circle No. 39 on Reader Information Card.

## Aluminum-Bronze Applications

Ampco Metal, Inc., Milwaukee 46, Wis., has published the second quarter 1958 issue of "Ampco Welding News." Included in No. 75 of this 8½ x 11-in. 4-page illustrated bulletin are eight articles principally devoted to the various applications of Ampco-Trode aluminum-bronze filler wires. Featured are "Rolling Mill Adjustment Screw Rebuilt with Ampco-Trode," "Forging Press Gear Hub Repaired with Ampco-Trode 10" and "Luxury Liners Use Ampco Metal to Combat Sea Water Corrosion."

For your free copy, circle No. 40 on Reader Information Card.

## Welding in Canada

Canadian Liquid Air Co. Ltd., 1111 Beaver Hall Hill, Montreal, Quebec, has published its June issue of "Arc and Flame," a 4-page pamphlet in color, containing such articles as "Simple Argowelding Equipment" and "Welding of Gas Tanks."

For your free copy, circle No. 41 on Reader Information Card.

## Specialized Heat Treatments

Ferrotherm Co., 1861 E. 65th St., Cleveland 3, Ohio, offers a new 8-page technical folder on "Heat Treatments in Atmosphere Furnaces." Data are included on protective atmosphere furnace annealing, brazing and hardening of such metals as stainless steel, 17-4, 17-7, beryllium and Inconel. Protective atmosphere data such as dissociated ammonia, hydrogen, exothermic, argon and helium are also included.

For your free copy, circle No. 42 on Reader Information Card.

## Catalog on Resistance Welders

Alphil Spot Welder Mfg. Corp., 1058 Pacific St., Brooklyn 38, N. Y., has published a 2-color catalog on its complete line of resistance welders specifically designed for the sheet metal, wrought iron, wire fabricating and tubing industries.

The catalog contains descriptions, specifications and applications for Alphil flash, projection, butt, spot and gun welders made for all voltages and frequencies.

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| 213 | 475 | 1400 |
| 225 | 488 | 1450 |
| 238 | 500 | 1500 |
| 250 | 525 | 1550 |
| 263 | 550 | 1600 |
| 275 | 600 | 1650 |
| 288 | 650 | 1700 |
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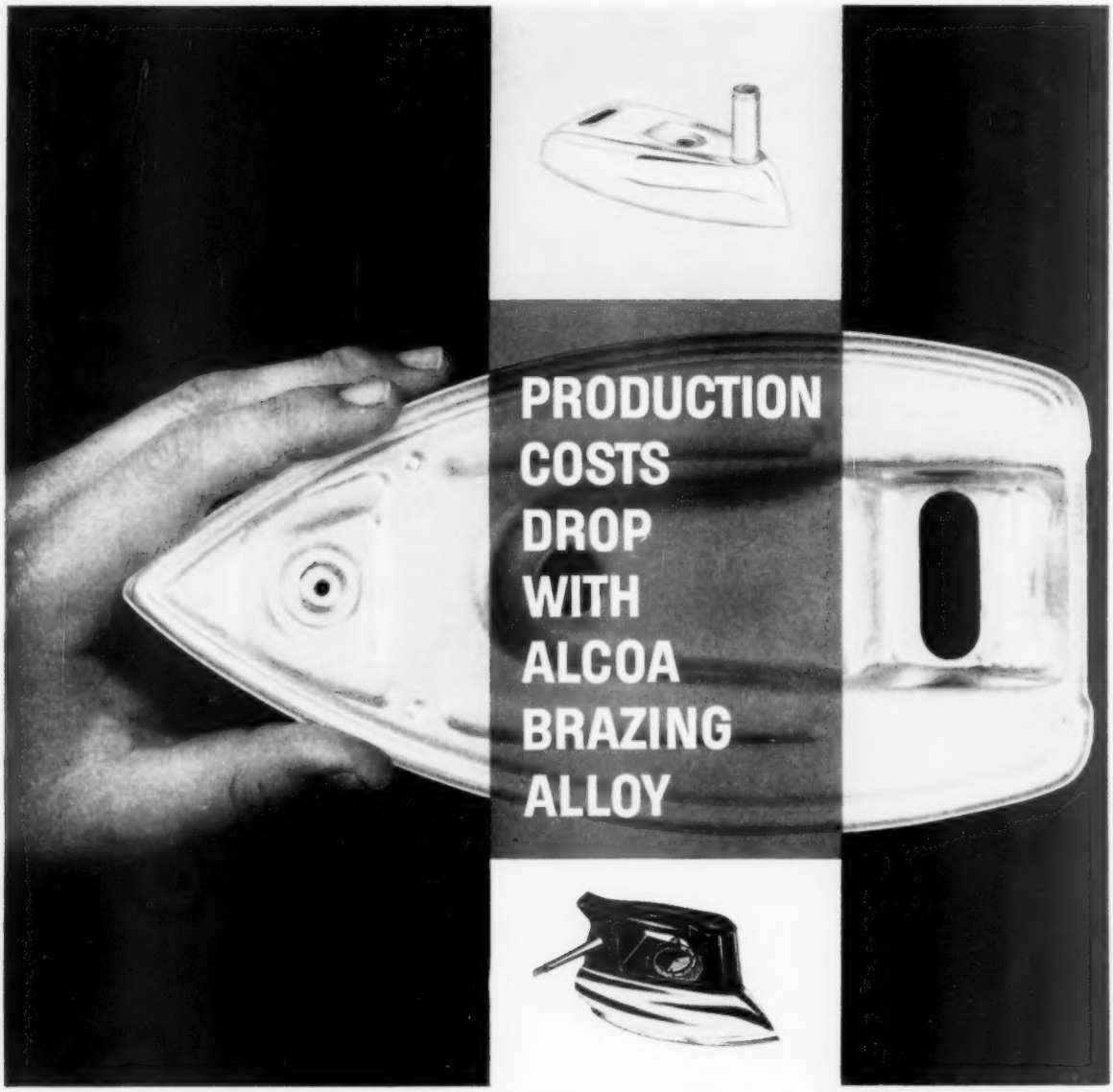


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Dominion's aluminum tank is equal in rigidity to stainless steel, more rigid than brass, yet weighs and costs only half as much as the other two materials. Hand application of filler metal and expensive chrome plating were eliminated in the switch to aluminum, and rejects are now few and far between. The new Dominion steam iron is lighter, heats faster, offers better protection against corrosion and tank perforation.

Alcoa is a never-ending source of *new and better* ways to weld, braze and solder aluminum. Write for FREE books and films on joining aluminum. Find out how aluminum can help you cut production costs and improve your product. For more information, contact one of the Alcoa distributors listed here, or write Aluminum Company of America, 1741-J Alcoa Building, Pittsburgh 19, Pennsylvania.

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Southern Oxygen Co.
Charlottesville
Southern Oxygen Co.
Lynchburg
Southern Oxygen Co.
Norfolk
Southern Oxygen Co.
Richmond
Southern Oxygen Co.
Roanoke
Southern Oxygen Co.

**WASHINGTON**
Seattle
Pacific Metal Co.
J. E. Haseltine & Co.
Spokane
J. E. Haseltine & Co.

**WEST VIRGINIA**
Bluefield
Southern Oxygen Co.

**WISCONSIN**
Milwaukee
Machinery & Welder Corp.
Steel Sales Corp.

**For details, circle No. 45 on Reader Information Card**

neering information on the Norelco EM-100B Electron Microscope is available from the Instruments Division, Philips Electronics, Inc., 750 S. Fulton Ave., Mount Vernon, N. Y.

The text covers construction details, electron optical system, electron source, condenser lens, objective lens and intermediate lens systems. Sections are included on the beam wobbler, electron optical alignment, electronic power supplies, vacuum system and photographic equipment including 35-mm, 2- x 2-in. plate and front-end cameras.

For your free copy, circle No. 46 on Reader Information Card.

## Accident Prevention

The National Safety Council, 425 N. Michigan Ave., Chicago 11, Ill., has released the second edition of its "Handbook of Accident Prevention." New material in the 93-page volume includes a description of radioactive hazards and ways to protect against them.

For more details, write to the Council at the above address.

## Press Brake Data

Bulletin No. 13-693 from the Taylor-Winfield Corp., Warren, Ohio, describes the firm's line of press brakes. It gives complete specifications on all machines, dimensions and force required to bend mild steel with standard "air bend" dies.

For your free copy, circle No. 47 on Reader Information Card.

# REVIEWS

OF NEW BOOKS

## Safety from Radiation Materials

*Safe Design and Use of Industrial Beta-Ray Sources*, National Bureau of Standards Handbook 66, issued May 28, 1958, 28 pages, 20 cents. Prepared under American Standards Association procedures. (Order from Superintendent of Documents, U. S. Government Printing Office, Washington 25, D. C.)

As industrial use of radioactive materials, X-rays and particle accelerators increases, it is essential that adequate precautions be taken to protect the user and the public against excessive exposure to radiation. This handbook has been composed to serve as a guide toward safe design, manufacture, installation, use, maintenance and disposal of beta-ray sealed sources for industrial applications.

Because of the present rapid expansion of this technology and likelihood of change, this handbook gives a generalized discussion of the many aspects of personnel protection in connection with beta-ray sources, rather than a set of rigid safety rules for their manufacture and use. It is hoped that the information contained herein will help the reader to comply with the radiation protection regulations issued by the U. S. Atomic Energy Commission and other federal and state agencies.

The book was prepared, under the sponsorship of the National Bureau of Standards, by the Subcommittee on Sealed Beta-Ray Sources of American Standards Association Z54 Sectional Committee, whose purpose is to formulate safety codes in which basic standards and other appropriate data are applied to industrial radiation protection problems.

The book contains a glossary of definitions, and chapters are included on radiation exposure, selection of radioactive materials, prototype testing, instruction manuals, labels, working conditions, accidents entailing radiation hazards and several chapters dealing with the handling of sources. There are two appendices: a listing of pertinent laws and regulations, and maximum permissible exposure limits.

Foreign remittances must be in U. S. exchange and should include an additional one-fourth the publication price to cover mailing costs.

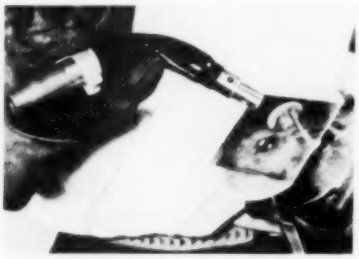
## Brazing-Alloy Development

*Development of Oxidation and Liquid Sodium Resistant Brazing Alloys.* D. A. Canonico and H. Schwartzbart, Armour Research Foundation for Wright Air Development Center, U. S. Air Force. Mar. 1958. 44 pages. (Order PB 131745 from OTS, U. S. Department of Commerce, Washington 25, D. C., $1.25.) Two advances were made during this work aimed at development of filler metals for brazing Type 310 stainless steel to Iconel, with the requirement that the brazements be resistant to oxidation and attack by molten sodium at 1650° F. Ninety-one iron-, chromium-, and nickel-based alloys were evaluated and 16 satisfied requirements for flow temperature (between 1750 and 1900° F) and flowability (minimum 3 in. along a 6-in. T-specimen). These alloys were subjected to an oxidizing atmosphere for 500 hr at 1650° F. Four of them withstood oxidation to a depth of less than 0.003 in. The second advance was development of a test for ductility of brazed T-joints in sheet material. The test was based on the premise that the load necessary to cause cracking in the fillet of a braze is a measure of the ductility of the brazing alloy. The test was capable of differentiating two filler alloys when used with statistical analysis.

# NEW PRODUCTS

## Gas-Shielded Gun

Hobart Brothers Co., Troy, Ohio, announce the development of a $CO_2$ inert-gas-shielded metal-arc welding gun. The new air-cooled semiautomatic welding gun is reported to increase welding speeds up to 200 ipm on ideally positioned fillet welds. Hobart discloses



that the portability of the unit together with the economy of using carbon-dioxide gas provides faster and more inexpensive production welds on such jobs as mild-steel auto frame subassemblies.

For more details, circle No. 48 on Reader Information Card.

## Stainless-Steel and Surfacing Wires

A new line of bare stainless-steel welding wire, plus an expanded line of manual and automatic hard-surfacing electrodes have been announced by the Welding Products Division, A. O. Smith Corp., Milwaukee, Wis.

The welding wire is offered in coils, spools and straightened and cut lengths. The electrode line consists of 19 different manual types and seven different tube type wires for semiautomatic and automatic applications.

For more details, circle No. 49 on Reader Information Card.

## High-Tensile Electrodes

The Lincoln Electric Co., Cleveland, Ohio, has announced two new electrodes for making high-strength joints in alloy steels. The electrodes, Jetweld LH-90 and Jetweld LH-110, are new iron-powder, low-hydrogen types designed to weld low-alloy and high-strength steels. Jetweld LH-90 is classed E-9018 and also meets the requirements of E-8018-B2. Jetweld LH-110 is classed E-11018. The low-hydrogen coatings with iron powder added increase the deposit rates and improve operating characteristics.

For more details, circle No. 50 on Reader Information Card.

## New Packaging for Welding Fittings

A program of individual packaging of its stainless-steel and nonferrous welding fittings and flanges for industrial piping was announced by Tube Turns, Louisville, Ky. division of Chemetron Corp.

Every stainless-steel and nonferrous alloy fitting in sizes up to and including 12 in. and all flanges up to and including 8 in. will be individually packed in a sleeve-type cardboard cylinder of a type built to government specifications for packing artillery shells.

Each container will be identified by a yellow Tube-Turn label indicating the nominal pipe size, description of the fitting or flange, wall thickness, type of metal and a code number indicating the job order. The containers have double-strength walls, are colored blue with yellow metal ends, deeply crimped.

For more details, circle No. 51 on Reader Information Card.

## Low-Hydrogen-Coated Nickel-Manganese Electrode

A new composite-coated hard-surfacing electrode is now available from Wall Colmonoy Corp., 19345 John R. St., Detroit 3, Mich.

Designated Walmang No. 3, the new nickel-manganese alloy is a work-hardening material said to provide good wear resistance and excellent impact resistance.

Walmang No. 3 operates on a-c or d-c reverse polarity. It has a low-hydro-

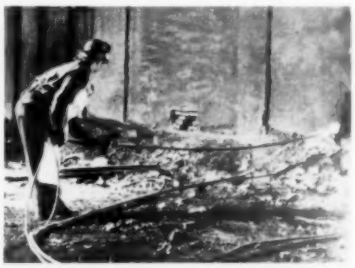
gen coating and is recommended for hard surfacing manganese and carbon-steel castings or forgings. It is particularly well suited to such applications as rail frogs and switches, crusher screens, coal crusher segments, shovel teeth, dredge parts, hammers and other equipment subject to heavy wear.

The material deposits at approximately 229 BHN; it work hardens in service to as high as 555 BHN. Walmang No. 3 conforms to AWS-ASTM Classification EFeMn-A covered and is packed for shipment in hermetically sealed steel cans.

For more details, circle No. 52 on Reader information Card.

## Powder Lancing

The Oxweld ACL-3 Powder Lance has been introduced by Linde Co., Division of Union Carbide Corp., 30 E. 42nd St., New York 17, N. Y.



Accurate control of intense heat is the key to operating the new ACL-3 Powder Lance. Lengths of standard black iron pipe are fitted into the front end of the ACL-3, which is connected to an oxygen supply and a source of special Oxweld metallic powder. The oxygen and metallic powder are mixed in the pipe and carried to the material being pierced or cut by the consumable pipe. This mixture is ignited at the end of the iron pipe, producing an extremely high temperature reaction that melts both

ferrous or nonferrous material in its path. On materials such as concrete, aluminum powder is added to the metallic powder to intensify the cutting reaction.

For more details, circle No. 53 on Reader Information Card.

## Neoprene Welding Cable

Belden Manufacturing Co., Chicago 80, Ill., has developed a new Corona-resistant neoprene-jacketed welding cable, designed specifically for use with gas-arc type, high-frequency welding

and a flexible neoprene-jacketed whip lead for use as an electrode lead. The manufacturer states that the same non-slip features that applied to their OTR cables have been built into the new neoprene cables, assuring a long lasting and durable jacket that won't slip back.

For more details, circle No. 54 on Reader Information Card.

## Combination Welding Outfit

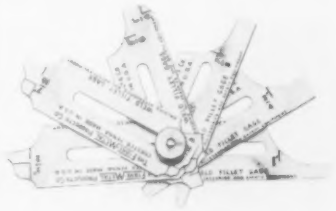
A welding torch, tips, cutting attachment and tip, oxygen and acetylene regulators and accessories are included in an all-purpose combination welding and cutting outfit announced by the

National Cylinder Gas Division of Chemetron Corp., 840 N. Michigan Ave., Chicago 11, Ill. Equipment is described as lightweight but of ample capacity for most welding, cutting, brazing, soldering or heating jobs in welding shops, farms, maintenance departments, garages, body and fender shops, blacksmith shops and industrial shops. An illustrated 73-page manual on oxyacetylene welding is included.

For more details, circle No. 55 on Reader Information Card.

## Fillet-Weld Gage

The Fibre-Metal Products Co., Chester Pa., have developed a "fillet-weld gage" for the measurement of nine weld



sizes from $\frac{1}{4}$ through 1 in. The gage is constructed from cold-rolled stainless steel and is 4 $\frac{1}{2}$ x 2 in. in size. Its weight is 3$\frac{1}{2}$ oz and comes packaged in a plastic case.

For more details, circle No. 56 on Reader Information Card.

## Oxygen Cylinder Trucks

Three new styles of cylinder trucks for handling medical oxygen have been developed by Harper Steel & Supply, Inc., Wichita, Kan. Finished in white enamel, they are constructed of all-welded lightweight steel tubing. All


New Clarkson Products
FOR WELDERS

WELD-GARD
STOPS WELD SPATTER
prevents adhesion of weld spatter to metal
non-toxic, non-inflammable, non-corrosive
apply with brush
spatter is wiped off without marring finish

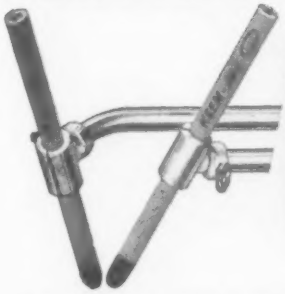
DEOXALUM
CLEANS AND DEOXIDIZES ALUMINUM FOR BETTER WELDS
a brush-on • wipe-off cleaner
one application removes both organic and oxide contaminants
provides clean, low electrical resistant surfaces

Send for complete information and samples.

Clarco
The Clarkson Laboratories, Inc.
930 N. Darien St., Philadelphia 23, Pa.
Welding Supply Dealers: dealers needed in some areas.

For details, circle No. 57 on Reader Information Card

BECKER
KEEN-ARC CARBONS



FOR CONSISTENTLY GOOD RESULTS

Designed to carry the high currents necessary for intense heat, BBB Keen-Arc Carbons produce a fine-grained weld of high tensile strength. They give a smooth, steady "flowing" flame which does not wander and which is concentrated at the desired focal point. Flame temperature is easily and accurately adjusted by merely changing the ampere input, and heavy copper coating permits gripping at extreme ends—eliminates frequent and periodic resetting.

*A COMPLETE LINE OF CARBON WELDING SUPPLIES including carbon and graphite electrodes, carbon rods and plates, welding paste, etc. Write for catalog.*

BECKER BROTHERS CARBON CO.
3450 South 52nd Ave. Cicero 50, Illinois

For details, circle No. 58 on Reader Information Card

models have large ball-bearing wheels in the front. Two models have, in addition, one or two small rear wheels,

respectively, which drop down automatically in normal use or may be locked out of position when the use of the front wheels only is required.

For more details, circle No. 59 on Reader Information Card.

### a-c/d-c Ammeter

A new "Tong Test a-c d-c Ammeter" has just been announced by the Columbia Electric Mfg. Co., Cleveland 14, Ohio. According to the manufacturer, Tong Test is the only instrument of its type that measures both ac and dc. Included in the engineering changes is a larger damping chamber for each scale range which is said to make readings easier by providing improved damping characteristics. Other new features are heavier and stronger scale range construction, insulated operating trigger and plastic handle.

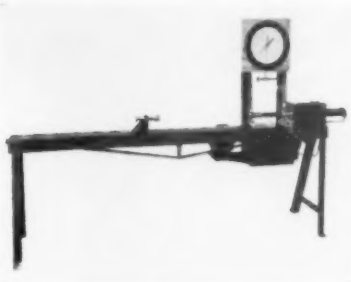
Tong Test is constructed without coils or windings so there is no danger of

burnouts. Tong Test is available in eight different types capable of measuring up to 1000 amp.

For more details, circle No. 60 on Reader Information Card.

### Tensile Tester

A new tensile testing machine has been developed by Detroit Testing Machine Co., 9390 Grinnell Ave., Detroit 13, Mich. It features a self-contained electrically driven power unit



and a long bed, permitting the testing of specimens up to five feet long. The machine can be reportedly set to test for any load up to 10,000 lb, the maximum developed by the hydraulic cylinder.

For more details, circle No. 63 on Reader Information Card.

### Utility Welding Goggle

The Dockson Corp., 3839 Wabash, Detroit, Mich., announces a new model, lightweight, general utility goggle to be known as the "Econolite."

The Econolite goggle eye cups are of injection-molded plastic. Integral deflecting louvers provide ventilation and keep out splash and flying particles. Welding models are equipped with


*Introducing . . . .*

**High-Temperature Brazing Flux**

**XCEL-FLUX "NF"**

**Chemically active up to 3000°F.**

Developed to meet new demands of industry for high-strength bonding of steel, stainless steel, Inconel and other high-temperature metals and alloys.

Manufacturers of XCEL-FLUX "SS" Paste. A leading quality flux for many years. Chemically active to 1850°F.

*Write for Technical Data and Samples*

**AMERICAN PRODUCTS CORPORATION**

525 S. Dearborn Street
Chicago 5, Ill.

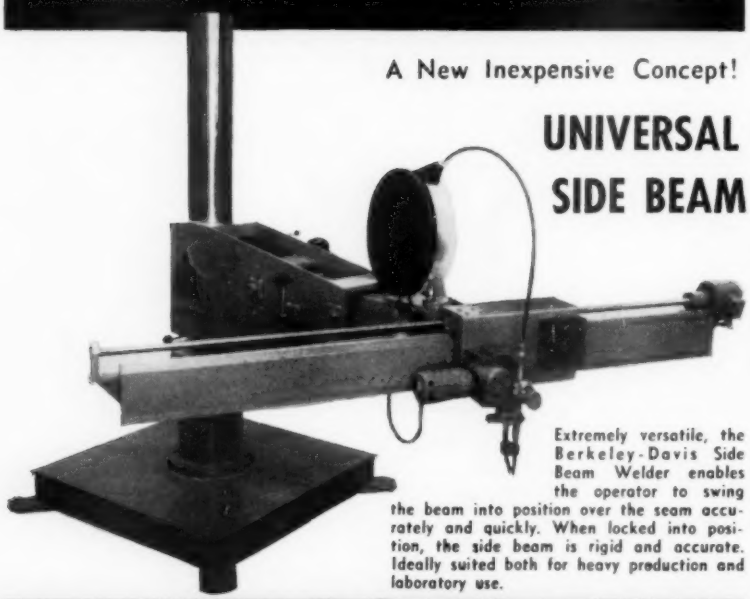
For details, circle No. 62 on Reader Information Card



**SOMETHING NEW FROM BD**

A New Inexpensive Concept!

**UNIVERSAL SIDE BEAM**

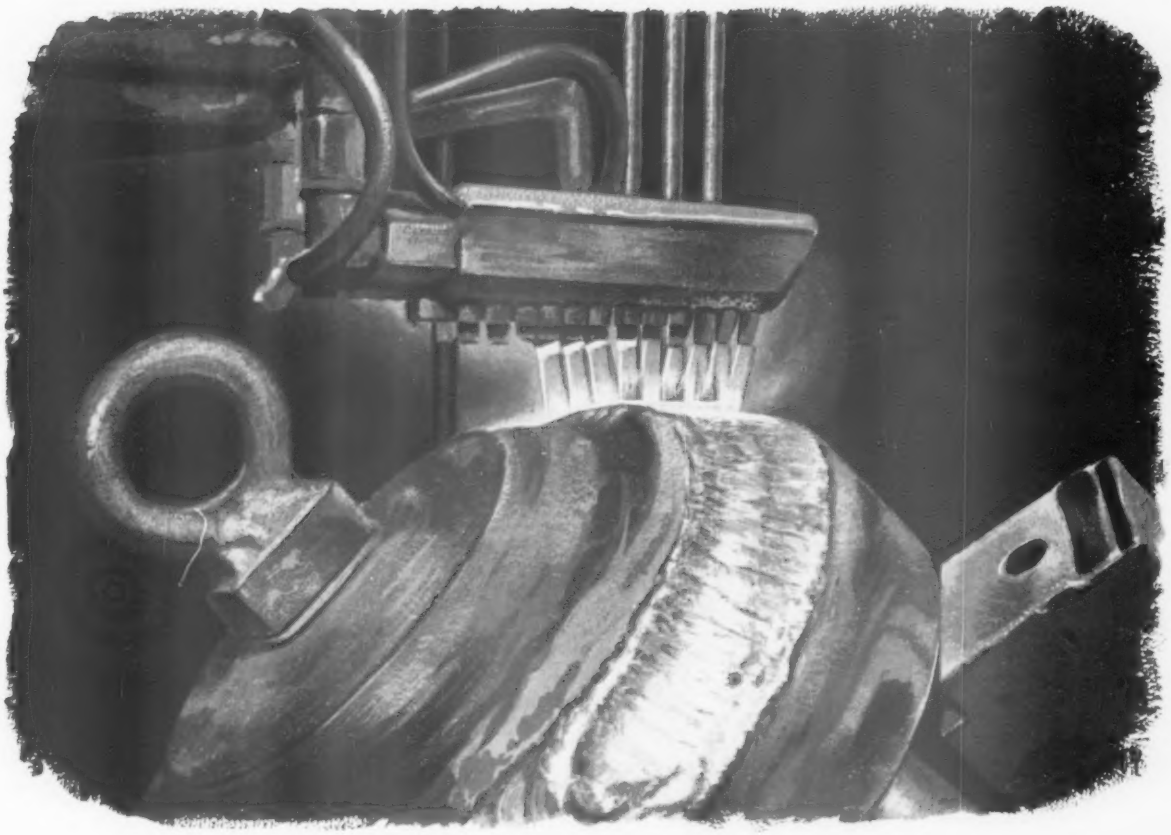


Extremely versatile, the Berkeley-Davis Side Beam Welder enables the operator to swing the beam into position over the seam accurately and quickly. When locked into position, the side beam is rigid and accurate. Ideally suited both for heavy production and laboratory use.

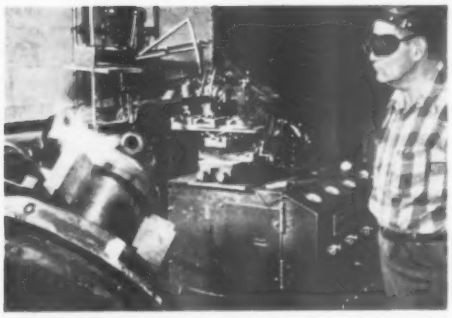
*Berkeley-Davis Incorporated* AFFILIATED WITH THE *Federal* MACHINE AND WELDER COMPANY

1021 BAHLS ST. • DANVILLE, ILLINOIS

For details, circle No. 61 on Reader Information Card



# *Mechanized Hard-Facing Saves Money*



A 25% saving in rod was realized by mechanizing the hard-facing of this valve disk. Time required to complete operation was cut from 2½ hours to ½ hour.

Now you can hard-face wearing parts on a production basis and *save* . . . with HAYNES Hard-Facing Machines.

The savings come from a number of factors. The mechanized method produces smooth, uniform deposits—fast. The smooth deposit reduces machining time and increases tool life. The uniform deposit cuts down on rejects and speeds production. Less oxygen and acetylene are required per part because the rate of hard-facing is fast and the flames are efficiently directed. Less rod is required because the depth of deposit is closely controlled.

Machines designed and produced by Haynes Stellite Company insure adequate preheat, proper proportioning of the welding flames, and a continuous supply of molten metal. Only a minimum of manual effort is required to turn out high-quality hard-faced parts.

Our field engineers can help you determine what hard-facing jobs in your shop are suitable for mechanization. For full information, write our general sales office in Kokomo, Indiana.

**HAYNES**
**ALLOYS**
**HAYNES STELLITE COMPANY**

*Division of*
*Union Carbide Corporation*
Kokomo, Indiana

UNION CARBIDE

"Haynes" and "Union Carbide" are registered trade-marks of Union Carbide Corporation.

For details, circle No. 64 on Reader Information Card

"Baffl-Vents" to protect the eyes from injurious flash rays.

Lenses are standard 50-mm size.

Eye cups are joined by a plastic-covered, adjustable, flexible ball and chain

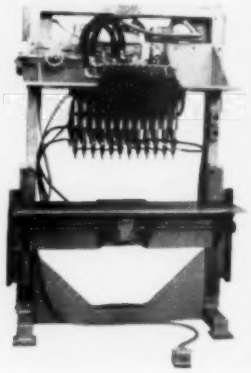
nose bridge, while an elastic head band is also provided.

The "Econolite" goggle is available in three types, for grinding and chipping, acid handling and welding.

For more details, circle No. 65 on Reader Information Card.

## Resistance Welding Cigarette Machines

A special 12-gun, air-operated welder used in assembling the magazine of cigarette vending machines, has been designed by the Taylor-Winfield Corp., Warren, Ohio. Fourteen rows of 12 spots each are used to assemble this product. The adjustable lower platen accommodates work fixtures of various

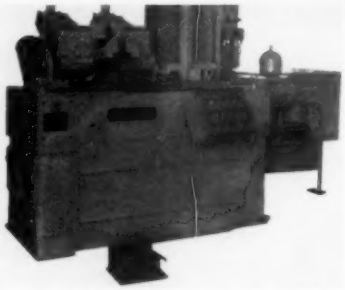


sizes. The guns can be positioned to meet varying assembly problems, welding 16 to 20 gage low-carbon steel. The machine is equipped with three 70-kva package-type transformers.

For more details, circle No. 67 on Reader Information Card.

## Automatic Flash Welder

A new line of automatic flash welders is now being marketed by Federal Machine and Welder Co., Warren, Co.



The 400 kva-F5 machine is for flash welding hot-rolled steel rings $^3/_8$ in. thick up to 8 in. wide. Air-operated alligator-type clamps provide necessary clamping force to hold the work. Upset force of approximately 38,000 lb is derived from a double acting hydraulic cylinder.

For more details, circle No. 96 on Reader Information Card.

## Water Flow Control for Resistance Welders

Van Vooren Products, 2133 9th St., East Moline, Ill., have designed a new styled water-saving control for use with resistance welders, induction heaters and other intermittently operated water-cooled equipment. This new Model TET-01 "Watermizer" is said to differ from earlier units in that the water flow is automatically regulated to machine load. Thermostats on machines or

For Years

HI-AMP®

has been FIRST with every important advancement made in

ELECTRODE HOLDERS

● Brilliant Red Tips and Trigger—Bright Yellow Handle — all Glass Fibre, an outstanding safety feature!

LENCO, INC.

Box 189, Jackson, Mo.

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NO PREHEAT
NO POSTHEAT

on

HIGH TENSILE STEEL

when you weld with

AIRCO 394

✻ Airco 394 all-position electrodes overcome underbead cracking in welding of most hardenable steels — without preheat.

✻ Make possible high quality hardenable steel welds without postheat.

✻ Weld steels of 30% or more carbon.

✻ Recommended wherever steels contain both alloying elements and high carbon.

• • •

Airco 394 electrodes produce a manganese molybdenum deposit of low hydrogen content. Weld metal is of high strength. Arc action is steady, beads are smooth, slag removal is easy.

FREE — Send for the handy Airco Electrode Guide. It will help you select the right electrode for your specific job. Request catalog 1318.

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AIRCO

AIR REDUCTION SALES COMPANY

A division of Air Reduction Company, Incorporated

150 East 42nd Street, New York 17, N. Y.

For details, circle No. 69 on Reader Information Card

*This 70-foot towboat, and two 120-foot boats, were constructed in stainless steel from the main deck down, for service at U. S. Steel Corporation Clairton Works, Pittsburgh.*

*Some of the 20 Vickers Controlarc welders in service at St. Louis Shipbuilding and Steel Co.*

# "Sewing a seam" in stainless steel with the VICKERS® *Controlarc* DC Welder

Vickers Controlarc DC Welders played an important part in an unusual project by St. Louis Shipbuilding and Steel Company: the construction of the first inland waterway towboats with stainless steel hulls. The three boats were built for service on the Ohio and Monongahela Rivers at U. S. Steel Corporation's Clairton Works, Pittsburgh.

A big feature of the Controlarcs' performance on this job was elimination of stub loss on stainless steel electrodes by *automatic* maintenance of a constant current setting from arc strike to end of pass. With a ton and a half of costly electrodes used on the 10,500 feet of welding, dollar savings were appreciable.

Suitability of the Controlarc for Arcair gauging and cutting was another reason for the choice of this machine.

For better performance on *any* welding job, the Vickers Controlarc magnetic amplifier-rectifier DC Welder provides automatic control of *current* and *type* of arc adaptable to changes in welding conditions. For complete information on this unusual welding machine, write today for Bulletins EPD 7116-1.

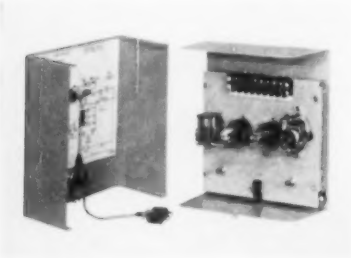
***expensive stub loss eliminated on stainless steel welding job***

ELECTRIC PRODUCTS DIVISION — VICKERS

**VICKERS INCORPORATED**
DIVISION OF SPERRY RAND CORPORATION
**ELECTRIC PRODUCTS DIVISION**
1853 LOCUST STREET • SAINT LOUIS 3, MISSOURI

For details, circle No. 70 on Reader Information Card

ignitrons can be connected to the terminals provided on the unit. The con-



trol panel can be mounted inside the machine if so desired.

For more details, circle No. 71 on Reader Information Card.

## Protective Coating

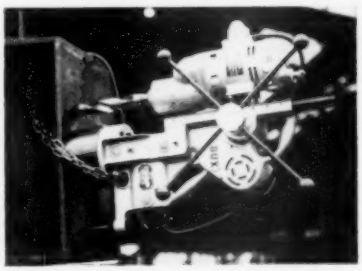
Industrial Metal Protectives, Inc., 433 Homestead Ave., Dayton 8, Ohio, have introduced a corrosion-resistant coating to permit conductivity through the protective coating without destroying corrosion resistance.

The coating, Zincilate 400, is claimed to have eliminated the problem of electrode contamination usually found when coatings are applied to the workpiece in spot welding.

For more details, circle No. 72 on Reader Information Card.

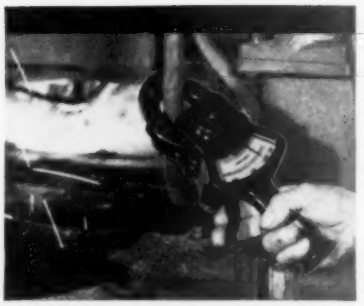
## Magnetic Press

A new portable magnetic drill press with automatic power feed has been announced by Buck Mfg. Co., 1355 N. 10th St., P. O. Box 692, San Jose, Calif. The new unit, called the "Bux-Matic," has a positive feed and two-speed control.



The Bux-Matic power feed is only slightly larger than its well-known counterpart, the manually operated Model L-3, which weighs only 51 lb. All makes and sizes of portable drills, both air-operated and electric, can be adapted to the new power feed by the use of Bux adapters. The unit is reportedly capable of drilling up to $1^3/_4$-in. holes while in any position.

For more details, circle No. 73 on Reader Information Card.

**Get the exact current for quality welding!**

**USE THE TONG-TEST AC-DC AMMETER**

Don't assume your welders are using the right amperage. *Be certain* by controlling welding quality with the Columbia Tong Test AC-DC Ammeter, the only clamp-type instrument that measures both alternating and direct current. Snap tongs around the electrode cable for instant readings. Available in five types with interchangeable ranges up to 1000 amperes. Impossible to read wrong scale. No coils or wires to burn out—no electrical connections needed.

COLUMBIA

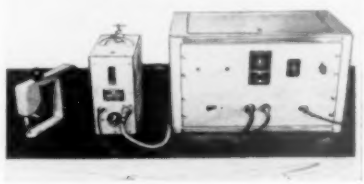
*Write for illustrated 8-page Catalog WJ-500*

**COLUMBIA ELECTRIC MFG. CO.**
**4513 Hamilton Avenue • Cleveland 14, Ohio**

For details, circle No. 75 on Reader Information Card


## Resistance-Welding Control

Vacuum Tube Products Co. of Oceanside, Calif., is producing a new combination welding control unit, complete with portable pneumatically operated welding hand gun.



The new unit consists of pneumatic portable weld head VTA-14 and air control unit VTA-15 used in conjunction with standard VTP 2.5-kva fractional cycle spot and seam welding control unit VTW-1.

The weld head (VTA-14) weighs 7 lb, is hand-operated and portable, and is capable of applying up to 75-lb weld pressure. The air control unit (VTA-15), in conjunction with the weld head, is said to provide precise pressure adjustment insuring exact duplicate welds capable of certification.

For more details, circle No. 76 on Reader Information Card.

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R. W. Creamer, V.P., checks x-ray films of tank welds.

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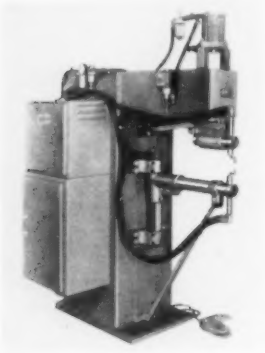
## Spot Welder

Precision roller-bearing ram, high-current electrical design and high speed are featured by the Model A-R resistance spot welders, designed for the sheet-metal, wrought-iron and wire fabricating industries by Alphil Spot Welder Mfg. Corp., 1058 Pacific St., Brooklyn 16, N. Y.



The roller-bearing ram reportedly permits bind-proof precision alignment of the work together with application of positive pressure. An independent air valve brings the ram down for fine electrode adjusting.

Current capacity is 75 kva with a silicone steel transformer, attaining to temperatures adequate for welding 16-gage commercial aluminum and 7-gage cold-rolled steel, and adjustable by an 8-step rheostat.

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## Surface Finishing

A coated abrasive finishing paper, known as Armour "No Load" paper, is available from the Armour Coated Abrasives Division, Armour and Co., Alliance, Ohio. This dry sanding product is recommended for use on non-ferrous metal surfaces, including aluminum. It is available in 9- x 11-in. sheets and in disks cut from this sheet.

For more details, circle No. 79 on Reader Information Card.

## Jaw Cleaner for Electrode Holders

Atlas Welding Accessories, Inc., 707 E. Lewiston, Detroit 20, Mich., have designed their Atlas "Jaw Cleaner" for use in cleaning corrosion, spatter and

dirt from electrode holder jaws without damaging the metal. The new product consists of a hardened steel broach and spiral wire brush. Such a polish is reported to maintain a perfect electrical contact.

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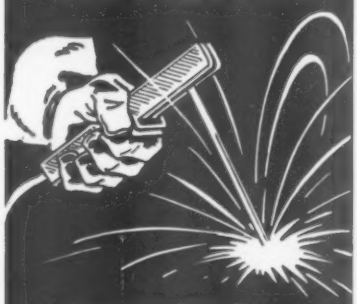

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ing Co., Palatine, Ill., "Swett-Ban" is made of a spongelike strip of cellulose acetate backed with Pellon fabric, plus a rubber headband.

For more details, circle No. 84 on Reader Information Card.

## Gas-Fired Hand Torch

A new line of commercial-fuel gas brazing and soldering torches has been introduced by Selas Corporation of America, Dresher, Pa. Five standard sizes are available which provide heat release from 500 to 32,800 Btu/hr. In

addition to brazing and soldering, the new torches are recommended for jewelry and dental manufacture, preheating and spot heat treating. The torch contains a perforated refractory screen to resist clogging and to maintain port size in addition to an alloy pilot ring to resist corrosion and oxidation.

For more details, circle No. 85 on Reader Information Card.

## Welding Stain Remover

Chemclean #536, an acid, colorless liquid, has been developed by Chemclean Products Corp., 610 Warren St., Brooklyn 17, N. Y., to remove burns and stains on stainless steel caused by welding. The manufacturer particularly recommends this product for the 300 series of stainless steels which, when used, will brighten the surface.

For more details, circle No. 86 on Reader Information Card.

## Aluminum Soldering Flux

American Solder & Flux Co., 19th and Willard Sts., Philadelphia 40, Pa., have introduced Amco #2316 flux, a thin, white, creamy paste which is said to be easily applied by a brush and may be used with Amco #3031 or other aluminum solders containing zinc, to join aluminum to aluminum or aluminum to other metals. On torch applications, the fluxing action begins above 650° F. On furnace or oven soldering, Amco #2316 is said to provide positive capillary action at temperatures of 750° F.

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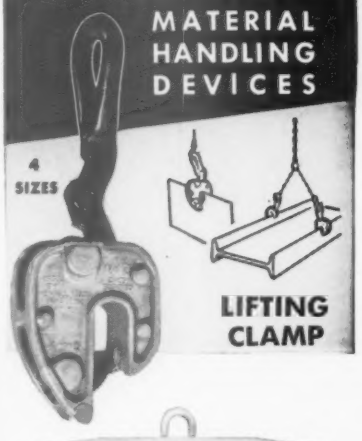


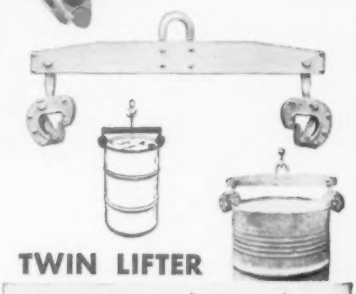

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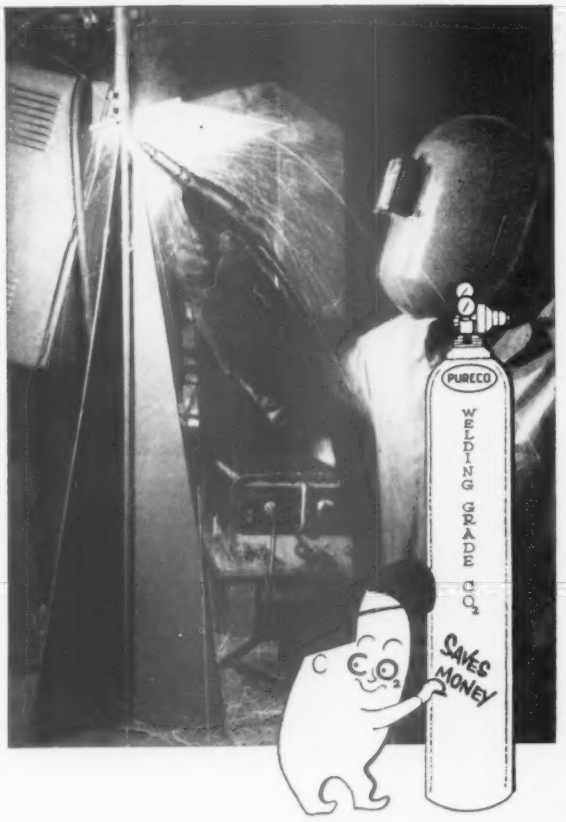


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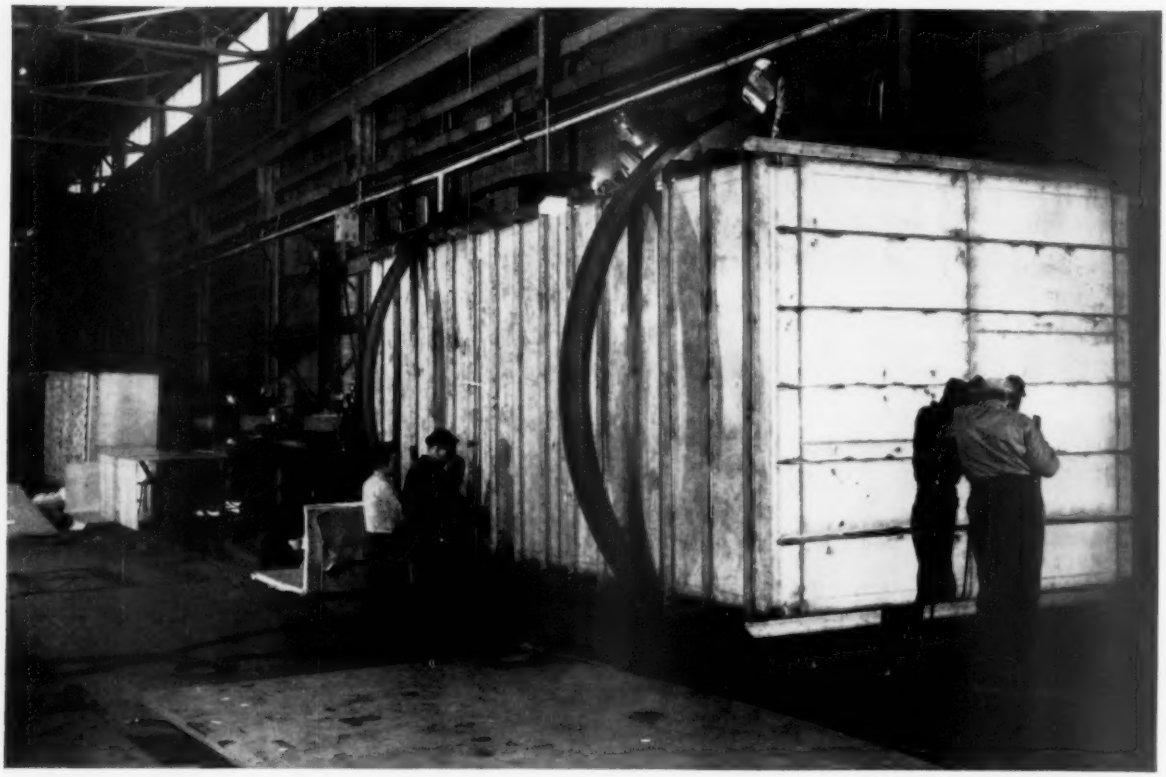

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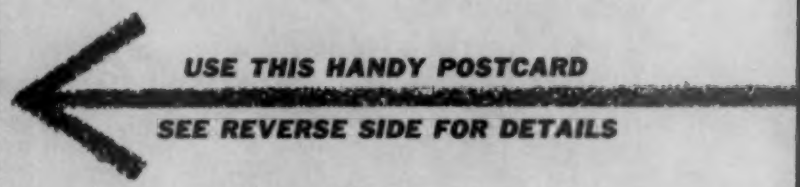


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# COMPARISONS BETWEEN WELDS IN IODIDE- AND SPONGE-BASE TITANIUM ALLOYS

*Paper describes the preparation of eighteen experimental alloys and presents the results from base-metal, weld-metal and weld-joint studies*

BY W. J. LEWIS, M. L. KOHN AND G. E. FAULKNER

INTRODUCTION. In a research program conducted at Battelle Memorial Institute under the sponsorship of Watertown Arsenal Laboratory, studies were made to compare the weld-joint properties of titanium alloys prepared from high-purity iodide titanium and commercial-purity sponge titanium. These studies were made to evaluate the effects of small amounts of interstitial elements on weld-joint properties. Alloys prepared with these base materials differed primarily in carbon and oxygen content. The sponge-base alloys contained about 0.05% more oxygen and 0.03% more carbon than the iodide-base alloys.

A series of nine alpha-beta alloys was prepared with each of the two base materials. The nominal compositions of the alloys were:

| | | |
|---|---|---|
| Ti-1.5% Fe | Ti-1.5% Mn | Ti-1.5% V |
| Ti-3.0% Fe | Ti-3.0% Mn | Ti-3.0% V |
| Ti-3.0% Fe-3.0 Al | Ti-3.0% Mn-3.0% Al | Ti-3.0% V-3.0% Al |

The amount of beta stabilizing elements in the alloys was limited to 3% so that ductile welds could be made without the use of special postweld heat treatments.

This paper describes the preparation of the experimental alloys and presents the results from base-metal, weld-metal and weld-joint studies.

## Alloy Preparation

Eighteen experimental alloys were melted and fabricated for this study. Nine alloys were prepared with high-purity iodide titanium as the base (iodide-base alloys), and nine were prepared with commercial-purity titanium sponge as the base (sponge-base alloys). High-purity vanadium, electrolytic manganese, electrolytic iron and commercially pure aluminum (99.7 to 99.9% aluminum) were used as the alloying elements. Master alloys were prepared with all of these elements except vanadium to aid in making the experimental alloys.

The experimental alloys were prepared by double melting procedures and were forged and rolled to 1/4- and 1/8-in. plate and sheet.

After cleaning, all of the alloy sheets and plates were vacuum annealed. The vacuum-annealing cycle was designed to: (1) control the hydrogen content of the alloys and (2) stabilize the alloys by causing maximum alpha precipitation from the beta phase. In this treatment, the alloys were heated to 1530° F under a dynamic vacuum of 1 micron or less and held at this temperature for 20 hr in a stainless-steel retort. During this treatment, the hydrogen content of the alloy was lowered to between 16 and 30 ppm by weight. After 20 hr at 1530° F, the specimens were furnace cooled to 1350° F, held at temperature for 1 hr, furnace cooled to 1150° F, again held for 1 hr and then cooled to room temperature by removing the retort from the furnaces. After the vacuum anneal, the specimens were clean and free of scale and were welded without further preparation.

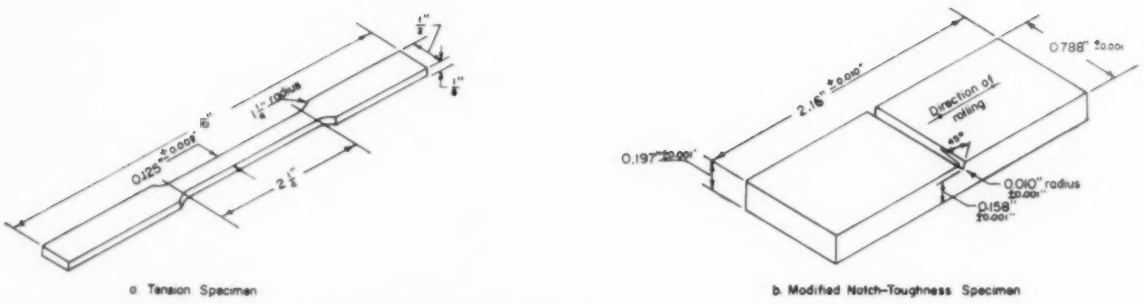
## Welding Procedures

Single-vee butt-welded joints were made in the 1/4- and 1/8-in. thick alloy plates and sheets. Edge preparation consisted of beveling the 1/4-in. plates by sawing to form a single vee with an included angle of 70 deg. The 1/8-in. sheets were beveled by grinding to form a single vee with an included angle of 90 deg.

In preparation for welding, the specimens were placed on grooved carbon-steel backing plates and held in place with copper bars and C-clamps. All of the welds were made manually with a water-cooled tungsten-arc torch using direct-current straight polarity in a welding chamber filled with helium. Although satisfactory welds may be made in air, a chamber was used to minimize oxygen and nitrogen pickup during welding.

Single-layer welds were made in the 1/8-in. sheet and two-layer welds were made in the 1/8-in. plate with current settings of 80 and 120 amp, respectively. Welding speeds were about 4 ipm. The filler metals had the same composition as the base metal and were prepared by shearing strips approximately 3/16-in. wide from the 1/8-in. thick sheets.

W. J. Lewis, M. L. Kohn and G. E. Faulkner are associated with the Battelle Memorial Institute, Columbus, Ohio.




a. Tension Specimen

b. Modified Notch-Toughness Specimen

Fig. 1 Specifications for tension and notch-toughness specimens

## Testing Procedures

Base-metal tension tests were made on flat-bar specimens machined from 1/8-in. sheets. The specimens were 6 in. long and had a 1/4-in. wide reduced section 2 1/2 in. long. A drawing of the specimen is shown in Fig. 1.

Guided-bend tests were made on the base metals and welded joints to determine their bend ductility. Both the base-metal specimens and welded joints were bent so that the strain was parallel to the direction of rolling of the plate. Weld-joint face-bend tests (longitudinal bend specimens) were made to show the bend ductility of the welded joint when the weld metal, heat-affected zone and base metal were forced to deform together. The specimens were 2 in. wide and 4 in. long, with the direction of welding parallel to the length of the specimen.

The guided-bend tests were made using dies of various radii and a vee block. The specimens were first bent over the die with the largest radius and, if no failure occurred, they were deformed over dies with successively smaller radii until the specimens failed. The bend ductility of the specimens made from 1/8-in. sheets was calculated from the smallest radius over which they could be bent before failure occurred. In the 1/4-in. plates, bend ductility was measured by the use of a grid which was stamped on the specimen with a special ink stamp.

Notch-toughness tests were made on welds in 1/4-in. plate using a modified vee-notch Charpy specimen. A schematic drawing of the specimen is shown in Fig. 1. The base-metal specimens were machined so that the notch was transverse to the rolling direction of the plate. In the welded specimens, the notch was machined in the center of the face of the weld. This modified specimen was used by Watertown Arsenal Laboratory in other titanium projects.[1] From tests conducted at Watertown Arsenal, the 10 ft-lb transition temperatures of commercially pure titanium determined with these specimens and standard vee-notch Charpy specimens were found to be about the same.

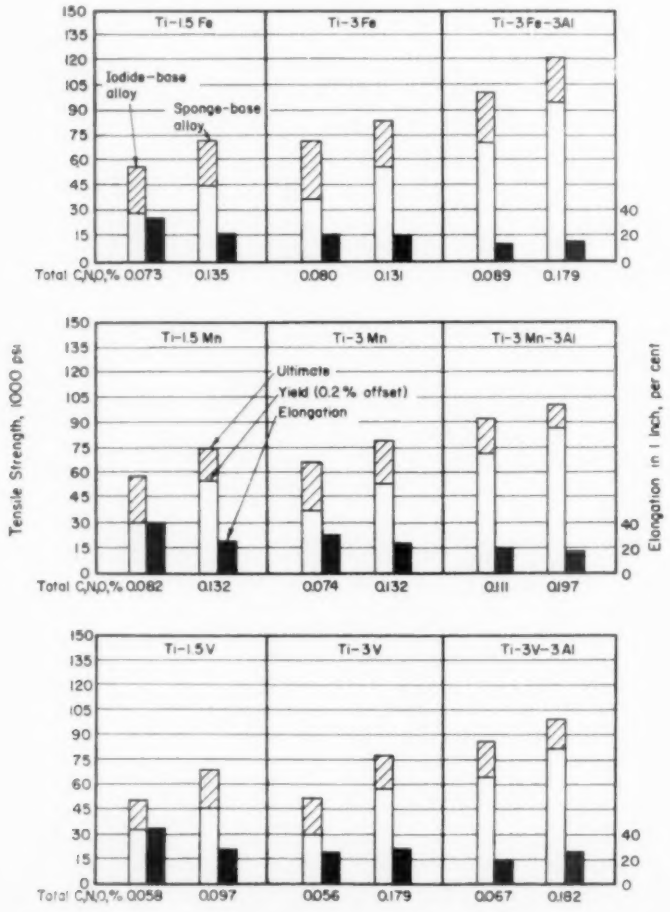



Fig. 2 Base-metal tensile properties of iodide- and sponge-base alloys

## Experimental Results and Discussion

In conducting the investigation, chemical and vacuum-fusion analyses were made to determine the substitutional and interstitial alloy content of the base metals. In addition, the weld metals were analyzed for oxygen, nitrogen and hydrogen. As anticipated, small unintentional variations occurred in both the interstitial and substitutional contents of many of the alloys. However, variations were taken into consideration in analyzing the data.

To facilitate discussion, the alloys are referred to by nominal substitutional alloy content. Also, the alloys are referred to by the base (iodide or sponge) which was used in preparing them.

The results presented in the succeeding sections are from single and duplicate tests. In most cases, tensile data are the results from single specimens and

Table 1—Chemical Composition of Iodide-Base and Sponge-Base Control Alloys

| Nominal alloy content, % (balance titanium) | Composition, % — Base metal | | | | | | | Weld metal | | |
|---|---|---|---|---|---|---|---|---|---|---|
| | Fe | Mn | V | Al | C | N | O | H | N | O | H |
| **Iodide-base alloys** | | | | | | | | | | | |
| 1.5 Fe | 1.46 | | | | 0.02 | 0.008 | 0.045 | 0.0020 | 0.014 | 0.072 | 0.0022 |
| 3.0 Fe | 3.22 | | | | 0.03 | 0.009 | 0.041 | 0.0031 | 0.008 | 0.074 | 0.0031 |
| 3.0 Fe, 3.0 Al | 2.53 | | | 3.19 | 0.02 | 0.011 | 0.058 | 0.0016 | 0.022 | 0.070 | 0.0025 |
| 1.5 Mn | 0.04 | 1.81 | | | 0.03 | 0.011 | 0.041 | 0.0025 | 0.009 | 0.042 | 0.0020 |
| 3.0 Mn | 0.02 | 2.81 | | | 0.03 | 0.010 | 0.034 | 0.0020 | 0.010 | 0.049 | 0.0024 |
| 3.0 Mn, 3.0 Al | 0.05 | 2.72 | | 3.10 | 0.05 | 0.008 | 0.053 | 0.0021 | 0.015 | 0.080 | 0.0028 |
| 1.5 V | 0.03 | | 1.40 | | 0.01 | 0.011 | 0.037 | 0.0026 | 0.010 | 0.041 | 0.0024 |
| 3.0 V | 0.04 | | 3.34 | | 0.01 | 0.007 | 0.039 | 0.0024 | 0.032 | 0.055 | 0.0032 |
| 3.0 V, 3.0 Al | 0.04 | | 3.23 | 3.13 | 0.01 | 0.009 | 0.048 | 0.0013 | 0.016 | 0.076 | 0.0017 |
| **Sponge-base control alloys** | | | | | | | | | | | |
| 1.5 Fe | 1.54 | | | | 0.04 | 0.007 | 0.088 | 0.0016 | 0.014 | 0.108 | 0.0013 |
| 3.0 Fe | 3.08 | | | | 0.04 | 0.009 | 0.082 | 0.0021 | 0.013 | 0.087 | 0.0026 |
| 3.0 Fe, 3.0 Al | 2.58 | | | 2.40 | 0.06 | 0.014 | 0.105 | 0.0015 | 0.021 | 0.108 | 0.0018 |
| 1.5 Mn | 0.08 | 1.51 | | | 0.03 | 0.018 | 0.084 | 0.0021 | 0.013 | 0.089 | 0.0023 |
| 3.0 Mn | 0.08 | 3.15 | | | 0.04 | 0.008 | 0.082 | 0.0025 | 0.009 | 0.088 | 0.0021 |
| 3.0 Mn, 3.0 Al | 0.10 | 3.20 | | 2.40 | 0.07 | 0.019 | 0.108 | 0.0021 | 0.017 | 0.102 | 0.0015 |
| 1.5 V | 0.07 | | 1.55 | | 0.01 | 0.009 | 0.078 | 0.0026 | 0.011 | 0.092 | 0.0021 |
| 3.0 V | 0.08 | | 2.93 | | 0.07 | 0.010 | 0.099 | 0.0020 | 0.012 | 0.096 | 0.0021 |
| 3.0 V, 3.0 Al | 0.10 | | 3.09 | 2.52 | 0.05 | 0.014 | 0.110 | 0.0022 | 0.018 | 0.108 | 0.0018 |

the bend and notch-toughness data are from duplicate specimens.

## Alloy Compositions

The nominal and actual compositions of iodide-base and sponge-base control alloys are listed in Table 1. Included also are the nitrogen, oxygen and hydrogen contents of the weld metals. The average interstitial contents of these alloys were as follows:

| | Composition, % — Nitrogen | Oxygen | Carbon | Hydrogen |
|---|---|---|---|---|
| Iodide-base alloys | 0.009 | 0.044 | 0.02 | 0.0022 |
| Sponge-base alloys | 0.011 | 0.092 | 0.05 | 0.0020 |

The compositions of the sponge- and the iodide-base alloys differed mainly in the amount of carbon and oxygen present. The amount sof metallic elements were similar, differing primarily in the alloys containing aluminum. In these alloys, the aluminum content of the iodide alloys ranged from 0.6 to 0.8% higher than the aluminum content of the sponge-base alloys. The effect of this difference in aluminum content could not be determined from the results.

From the weld-metal analyses, small amounts of interstitial elements were picked up or lost during welding. The nitrogen content of the weld metal ranged from 0.0005% less to 0.025% greater than that of the base metal; the oxygen content ranged from 0.006% less to 0.028% greater than the base metal; and the hydrogen content from 0.0005% less to 0.0009% greater than the base metal.

### *Tensile Properties*

Results from tension tests on the iodide- and sponge-base alloys are shown in Fig. 2. The tensile strengths of sponge-base alloys averaged 15,000 to 18,000 psi higher than those of the iodide-base alloys. This higher strength is due to the strengthening effect of the interstitial elements. These elements are known to be very potent strengtheners in titanium.

The differences in interstitial content between the iodide- and sponge-base alloys also affected tensile elongation. In the alloys which contained 1.5% beta stabilizers, the higher interstitial content of the sponge-base alloys was accompanied by lower tensile elongation (12 to 17%). However, in the alloys which contained 3% beta-stabilizing elements, singly and in combination with aluminum, the elongation of the iodide- and sponge-base alloys was about the same. Apparently, the effect of the higher interstitial content on tensile elongation was less pronounced as the total amount of substitutional-type alloying elements increased.

The tension-test results also showed that additions of substitutional elements

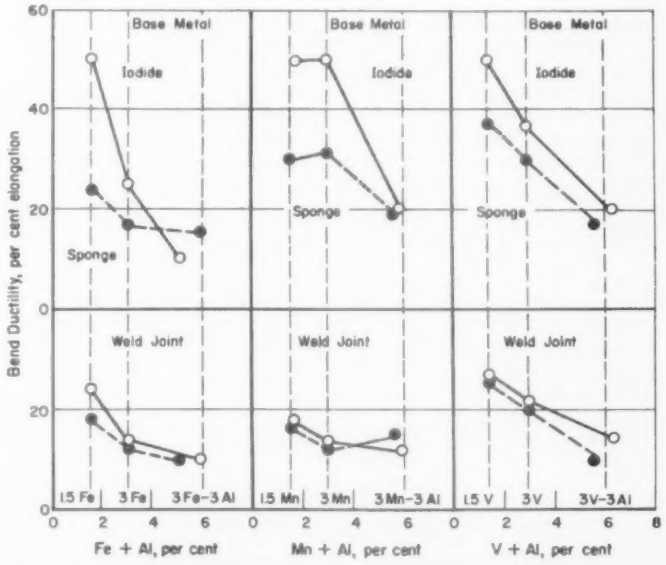



Fig. 3 Comparison between the base-metal and weld-joint bend ductility of iodide- and sponge-base alloys

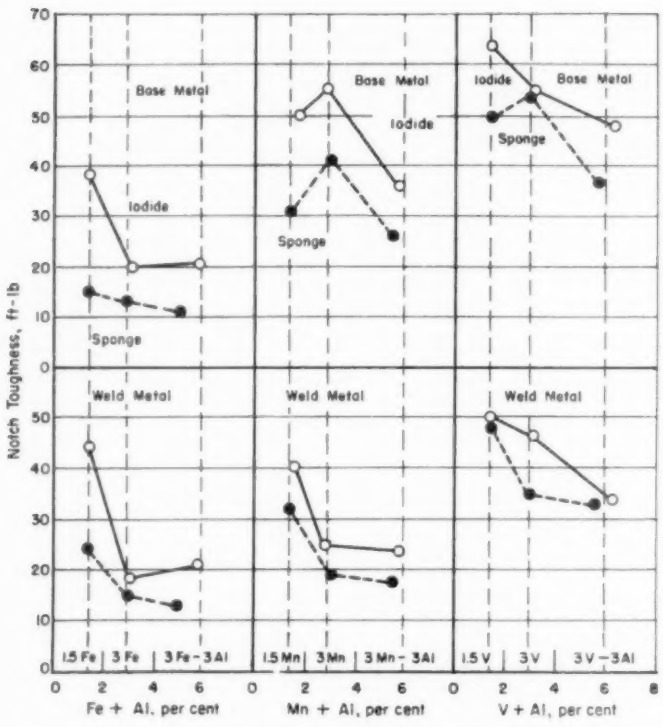



Fig. 4 Comparison between the base- and weld-metal room-temperature notch toughness of the iodide- and sponge-base alloys

increased the strength of the alloys. In general, those alloys containing vanadium had lower ultimate and yield strengths than the iron or manganese alloys. Except for the alloys containing aluminum, the iron- and manganese-bearing alloys had similar strength properties.

Although the strengthening effects of the substitutional elements were marked, the small difference in interstitial content between sponge- and iodide-base alloys was accompanied by greater strengthening than additions of 1.5% iron, manganese or vanadium. These small amounts of interstitial elements also had about one-half as much strengthening effect as did additions of 3% aluminum. Increasing the amounts of substitutional elements in the alloys lowered the tensile elongation of many to a greater extent than did the increased interstitial contents. The loss accompanying substitutional alloy additions was especially pronounced in the iodide-base alloys.

### Bend Ductility

The data from the bend tests are plotted in Fig. 3. For all but one alloy, the base-metal bend ductility was lower for the sponge-base alloys than for the iodide-base alloys. However, these adverse effects tended to become less pronounced as the total substitutional content of the alloys increased.

The weld-joint bend ductility of most of the sponge-base alloys was about the same as that of similar iodide-base alloys. Apparently, at these interstitial levels, bend tests on welded joints were not sufficiently sensitive to show effects due to the difference in interstitial content between the base metals.

The substitutional elements also had adverse effects on the bend ductility of the alloys. Although these effects varied for the different alloys, they followed trends similar to those that have been reported previously. These trends were: (1) increased beta content generally resulted in lowered base-metal and weld-joint bend ductility, (2) the addition of aluminum to the alloys lowered base-metal bend ductility considerably, but only had a small effect on the weld-joint bend ductility of the alloys which contained iron and manganese and (3) the alloys which contained vanadium alone generally had higher weld-joint bend ductility than those containing iron or manganese. The considerable decrease in weld-joint bend ductility caused by aluminum additions to the vanadium alloys was not noted in previous work. Because of this decrease, the bend ductility of joints in the Ti-3V-3Al alloys was about the same as that of comparable titanium-iron and titanium-manganese alloys.

The bend ductility of welded joints was lower than that of the base metals. This loss of ductility caused by welding is normal due to the heat-treatment response of the alpha-beta alloys and the cast structure of the weld metals. Welded joints in all of the alloys had over 8% bend elongation (6T minimum bend radius). This ductility is believed to be satisfactory for alloys of this type.

### Notch-Toughness Properties

As shown in Fig. 4, the interstitial and substitutional elements also had detrimental effects on the notch tough-

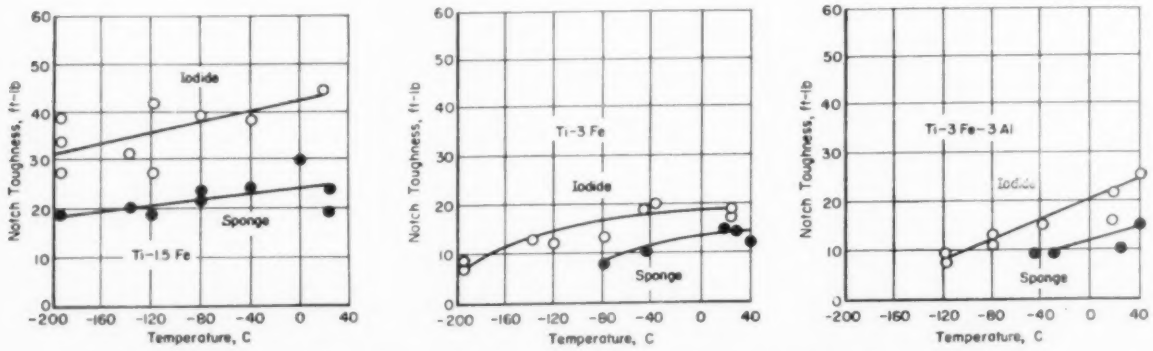



Fig. 5 Comparison of weld-metal notch toughness of iodide- and sponge-base titanium-iron alloys

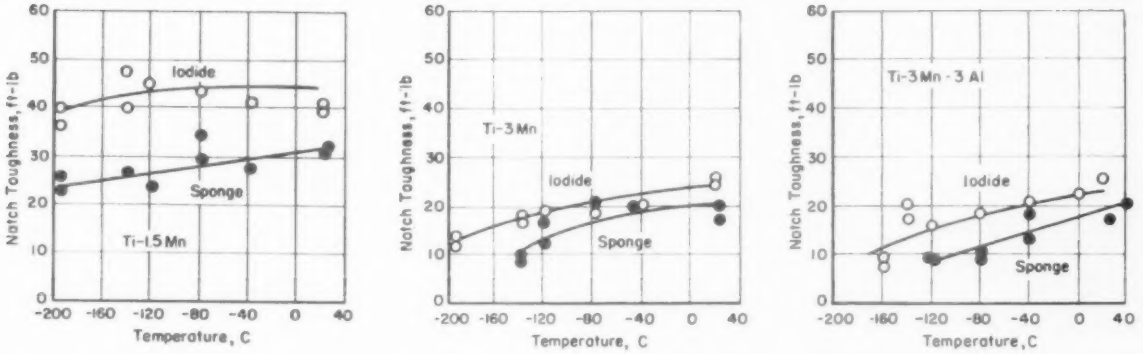



Fig. 6 Comparison of weld-metal notch toughness of iodide- and sponge-base manganese alloys

ness of the alloys. These effects were similar to those observed in the bend-test data. However, weld-metal notch-toughness tests appeared to show the effects of interstitial and substitutional elements more than weld-joint bend tests.

In all cases, the base- and weld-metal notch toughness of the sponge-base alloys was lower than that of iodide-base alloys. The lowered weld-metal notch toughness ranged from 6 to 8 ft-lb for the titanium-iron alloys, 3 to 13 ft-lb for the titanium-manganese alloys, and 1 to 12 ft-lb for the titanium-vanadium alloys.

The deterimental effects of interstitial and substitutional elements on toughness varied for the different alloys. However, the general trends were:

1. Sponge-base alloys which contained 1.5% beta stabilizers had lower base-metal notch toughness and higher weld-metal notch toughness than iodide-base alloys which contained 3% beta-stabilizing elements.

2. Most of the sponge-base alloys which contained 3% beta stabilizers had higher base-metal notch toughness and lower weld-metal notch toughness than iodide-base alloys which contained 3% beta stabilizers plus 3% aluminum.

3. The base- and weld-metal notch toughness of the titanium-vanadium alloys was higher than that of the other alloys, and the base-metal notch toughness of most titanium-iron alloys was low. The titanium-iron alloys would not be so desirable for applications requiring good notch toughness as the other alloys.

From trends (1) and (2), a comparison between the detrimental effects of interstitial and substitutional elements on base- and weld-metal toughness can be made. From (1), it appears that the difference in interstitial content between iodide- and sponge-base alloys lowered base-metal notch toughness more and weld-metal notch toughness less than an increase from 1.5 to 3% in beta stabilizers. From (2), it appears that this difference in interstitial content lowered base-metal notch toughness less and weld-metal notch toughness more than an addition of 3% aluminum to the alloys which contained 3% beta stabilizers.

With the exception of the iron alloys, the notch toughness of the weld metals was lower than that of the base metals. This lower notch toughness is attributed to the cast structure of the weld metals and the heat-treatment response of the alpha-beta alloys. The notch toughness of weld metals in the iron alloys was slightly higher or about the same as that of the base metals. Apparently, acicular transformed-beta structures in the iron alloys have higher notch toughness than structures formed by rolling and stabilizing the alloys at temperatures in the alpha-beta field. This observation has been made by other investigators.[2]

The effects of temperature on the base-metal notch toughness of the alloys are shown in Table 2. The effects of temperature varied with alloy content. For the alloys which contained 3% iron or manganese, singly or in combination with aluminum, the notch toughness at −40° F tended to be lower than at room temperature, especially for the Ti – 3Mn–3Al sponge-base alloy. With 1.5% iron or manganese present, the base-metal notch toughness was about the same at both temperatures, and the notch toughness of the vanadium alloys tended to be higher at −40° F than at room temperature.

The weld-metal notch toughness of the iodide- and sponge-base alloys at various temperatures is plotted in Figs. 5, 6 and 7. In most cases, lowering the temperature had similar effects on the weld-metal notch toughness of both the sponge- and iodide-base alloys. As the temperature was lowered over the range studied, the notch toughness of weld metals in practically all of the iron and manganese alloys decreased, but the notch toughness of only the vanadium alloy which contained aluminum decreased. From the curves and the data in Table 2, it appears that temperature

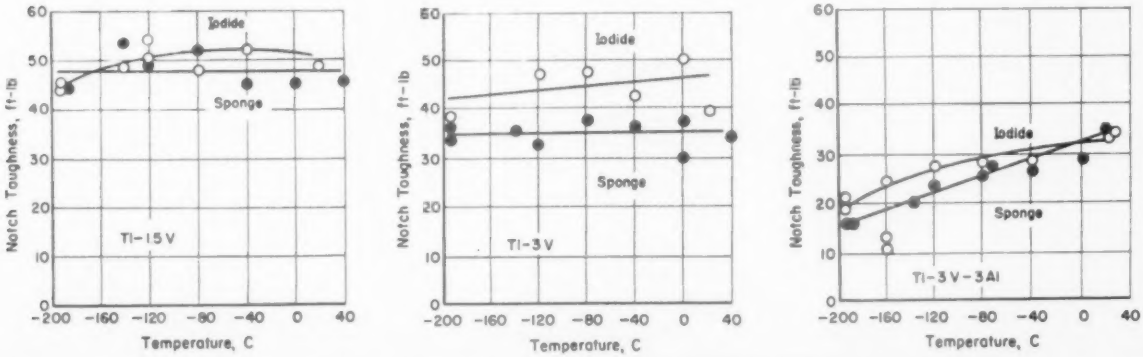



Fig. 7 Comparison of weld-metal notch toughness of iodide- and sponge-base titanium-vanadium alloys

Table 2—Base-Metal Notch Toughness of Sponge- and Iodide-Base Alloys at Room Temperature and —40° F

| Nominal alloy content, % (balance titanium) | Notch toughness, ft-lb: Iodide alloys, Room temperature | Iodide alloys, −40° F | Sponge alloys, Room temperature | Sponge alloys, −40° F |
|---|---|---|---|---|
| 1.5 Fe | 37, 39 | 40, 35 | 15, 15 | 16 |
| 3.0 Fe | 21, 19 | 17, 18 | 13 | 11 |
| 3.0 Fe, 3.0 Al | 21, 20 | 11 | 11 | 9 |
| 1.5 Mn | 50, 51 | 52, 52 | 32, 30 | 32, 32 |
| 3.0 Mn | 55, 55 | 53, 54 | 42, 40 | 33, 38 |
| 3.0 Mn, 3.0 Al | 34, 38 | 25, 21 | 32, 20 | 14, 13 |
| 1.5 V | 63, 64 | 67, 68 | 50, 50 | 54, 56 |
| 3.0 V | 55, 54 | 62, 60 | 52, 56 | 57, 58 |
| 3.0 V, 3.0 Al | 44, 52 | 49 | 36, 38 | 33, 39 |

has less of an effect on the titanium-vanadium alloys than on the titanium-iron and titanium-manganese alloys and that the influence of temperature on notch toughness is increased as the substitutional alloy content is increased.

All of the alloys containing vanadium had higher weld-metal notch toughness than similar alloys which contained iron or manganese. At temperatures as low as −200° C, the notch toughness of weld metals in the titanium-vanadium alloys exceeded 10 ft-lb; whereas, the weld-metal notch toughness of many of the titanium-iron and titanium-manganese alloys dropped below 10 ft-lb before this low temperature was reached.

The temperatures (transition temperatures) at which the weld-metal notch toughness of the alloys corresponded to 10 ft-lb are listed in Table 3. In all cases where a transition temperature was obtained, it was higher for the sponge-base alloys than for the iodide-base alloys. The difference between the weld-metal transition temperature of the sponge- and iodide-base alloys varied somewhat with alloy content. For example, the transition temperatures of the alloys which contained aluminum were higher than those of alloys which did not contain aluminum. However, the transition temperatures of the aluminum-bearing alloys were raised to a lesser extent because of the detrimental effects of interstitial content than similar alloys which did not contain aluminum. On the basis of transition temperature, alloys which contained 3% iron, singly and in combination with aluminum, were inferior to the titanium-manganese and titanium-vanadium alloys. Here again, alloys which contained 3% iron would not be desirable for applications requiring good notch toughness.

Table 3—Ten-Ft-Lb Transition Temperature of Weld Metals in the Iodide- and Sponge-Base Alloys

| Nominal alloy content, % (balance titanium) | Transition temperature, ° C: Iodide alloys | Sponge alloys |
|---|---|---|
| 1.5 Fe | <−200 | <−200 |
| 3.0 Fe | −170 | − 65 |
| 3.0 Fe, 3.0 Al | −100 | − 40 |
| 1.5 Mn | <−200 | <−200 |
| 3.0 Mn | <−200 | −140 |
| 3.0 Mn, 3.0 Al | −160 | −120 |
| 1.5 V | <−200 | <−200 |
| 3.0 V | <−200 | <−200 |
| 3.0 V, 3.0 Al | <−200 | <−200 |

## Conclusions

Comparisons between the tensile strength, weld-joint bend ductility, and weld-metal notch toughness of the iodide- and sponge-base alloys, indicated the following trends:

1. Increased interstitial- and substitutional-alloy content strengthened the alloys, but weld-joint bend ductility and weld-metal notch toughness were adversely affected.

2. Starting with iodide-base alloys which contained 1.5% beta stabilizers, the tensile and yield strengths were almost doubled (an increase of 40,000 to 60,000 psi) by combining the strengthening effects of the higher interstitial content of the sponge-base alloys, a 1.5% beta-stabilizing addition and a 3% aluminum addition. This strengthening was accompanied by a decrease in weld-joint bend ductility ranging from about 3 to 17% elongation and a decrease in weld-metal notch toughness ranging from about 18 to 32 ft-lb. In practically all cases, the decrease in ductility and toughness could be tolerated.

3. The strengthening afforded by the increase in interstitials from the iodide- to sponge-base level was more than or equal to that obtained by increasing the beta-stabilizing content of the alloys from 1.5 to 3%, but was less than that obtained by adding 3% aluminum to the alloys which contained 3% beta stabilizers.

4. The decrease in weld-joint bend ductility and weld-metal notch toughness which accompanied the increase in interstitials from the iodide to sponge level was less than that caused by increasing the content of beta stabilizers from 1.5 to 3%, but was more than that caused by adding 3% aluminum to the alloys.

5. For applications requiring good notch toughness, titanium-vanadium alloys would be more desirable than the others investigated and the titanium-iron alloys would be the least desirable.

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# CORRELATIONS OF BRITTLE-FRACTURE SERVICE FAILURES WITH LABORATORY NOTCH-DUCTILITY TESTS

*Nil-ductility transitions determined by drop-weight tests are demonstrated to correlate with the service performance of all materials investigated*

BY P. P. PUZAK, A. J. BABECKI AND W. S. PELLINI

ABSTRACT. Steels from various ship and nonship fractures which had experienced brittle fractures in service were investigated by conventional laboratory notch-bend tests and by the Naval Research Laboratory (NRL) drop-weight tests. The materials involved in the fractures included plain-carbon steels from ships; A285 plain carbon and A212 carbon-silicon steels from pressure vessels; A201 and A302 Mn-Mo steels from pressure vessels; A293 Ni-Mo-V steels from a pressure vessel and a turbine spindle; A293 Cr-Mo-V steel from retaining rings; 12% Cr cast martensitic stainless steels from ship propellers; and quenched-and-tempered 3½% Ni, 1% Cr steel from forged machinery components. The nil-ductility transitions (NDT) determined by drop-weight tests are demonstrated to correlate with the service performance of all materials which were investigated. As predicted by the drop-weight tests, the failures occurred at temperatures below the established NDT temperature in each case.

Charpy-V values of energy, fracture appearance and lateral expansion at the NDT temperatures are shown to vary with the type of steel. Similar variations occur on correlation with the temperatures of failure. The finding that brittle fractures may initiate at temperature levels corresponding to the Charpy-V 20 ft-lb temperature is of particular significance. This possibility was predicted by drop-weight tests prior to the establishment of service-failure documentation.

P. P. Puzak, A. J. Babecki and W. S. Pellini are associated with the Metallurgy Division of the U. S. Naval Research Laboratory, Washington 25, D. C.

## Introduction

Consequent to the unprecedented number of brittle failures which occurred in World War II welded cargo ships and tankers, the Metallurgy Division of the Naval Research Laboratory undertook a systematic study of the mechanical and metallurgical aspects of brittle fracture. These studies included the development of novel tests featuring ultra-sharp notches resulting from the cracking of a brittle weld termed "crack-starter weld." The initial studies were conducted by means of explosion loading

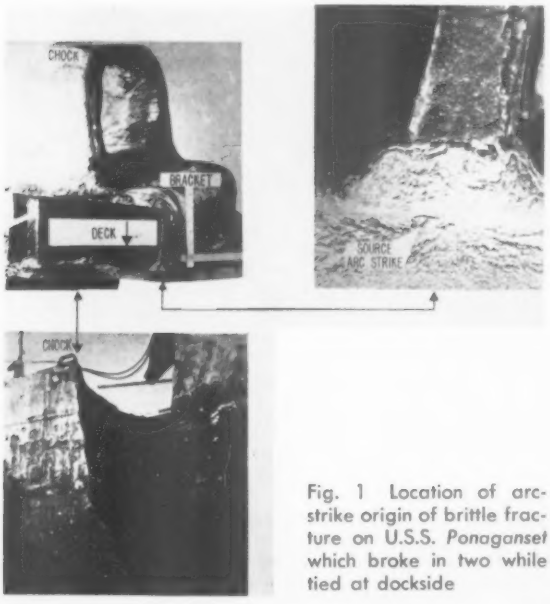


Fig. 1 Location of arc-strike origin of brittle fracture on U.S.S. *Ponaganset* which broke in two while tied at dockside

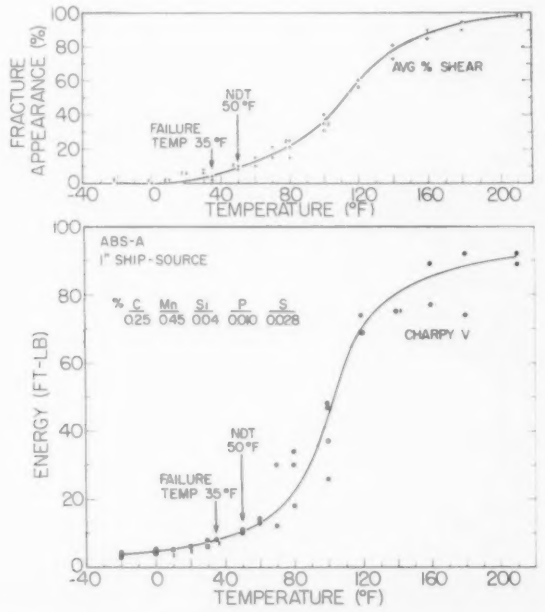


Fig. 2 Charpy-V-notch test data of Correlation Case No. 1 steel

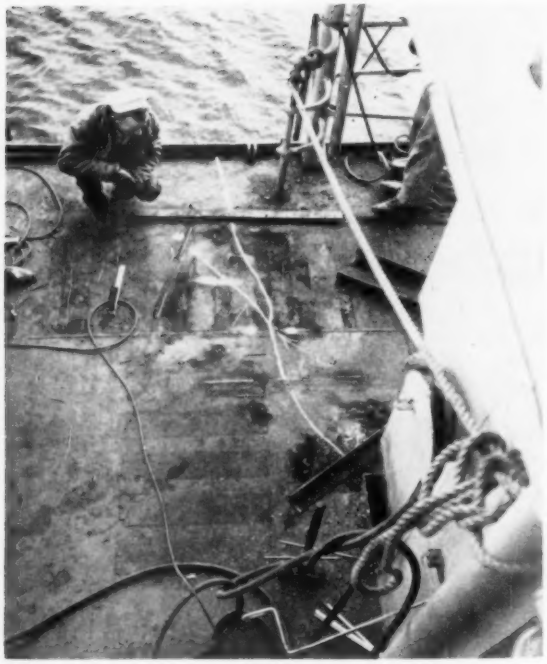

Fig. 3 Brittle crack in deck of *U.S.S. Markab* originating from square vent hole at lower right

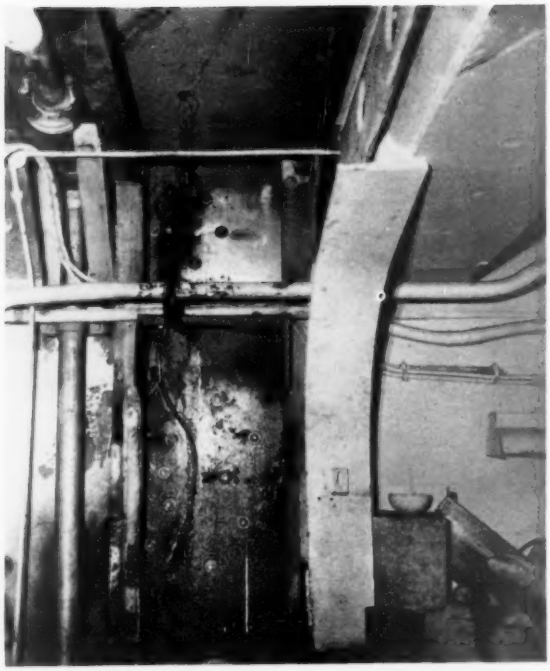

Fig. 4 The *Markab* crack as viewed from the ship's interior showing propagation through the top shell plate

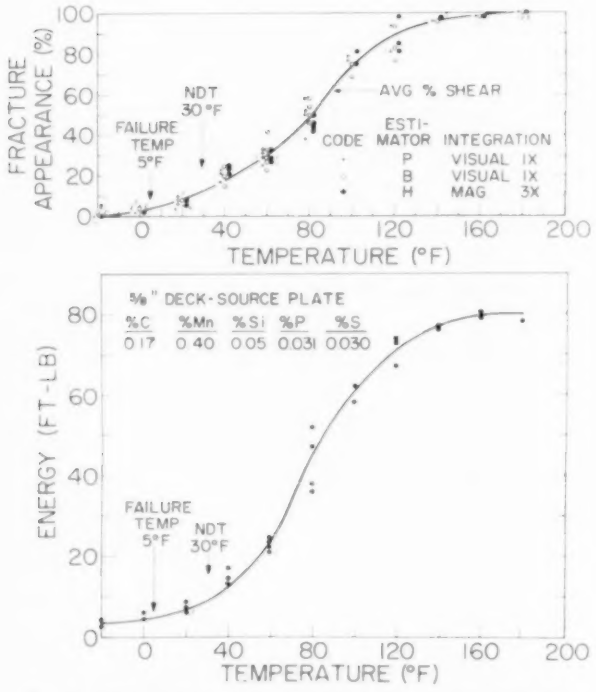


Fig. 5 Charpy-V-notch test data of Correlation Case No. 2 steel

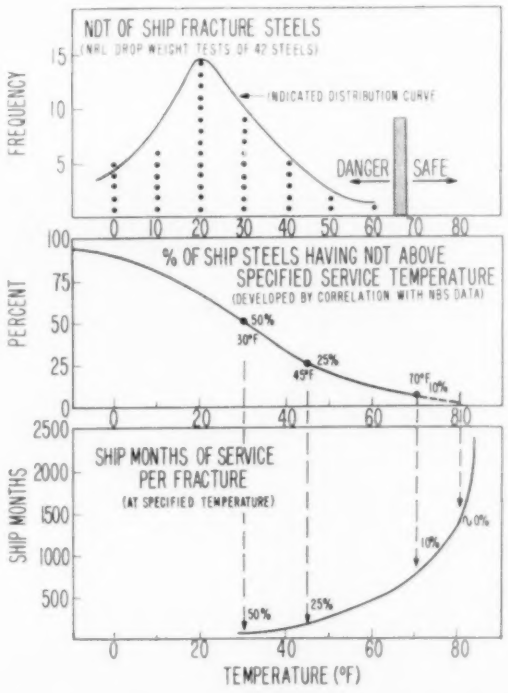


Fig. 6 Statistical distribution of NDT for ship-fracture steels, and correlation with actual ship-service performance

which permitted extensive investigations of the crack initiation and propagation characteristics of large plate samples of ship steels 3/4 to 1 in. thick without the requirement for large-scale tensile-testing facilities.[1]

The results of these explosion crack-starter tests were correlated with the National Bureau of Standards data of fractured ship plates,[2] and it was shown that brittle fractures were developed in the range of ship-failure temperatures. Additional correlations for explosion crack-starter tests with Charpy-V data indicated that crack initiation, propagation and termination corresponded to the critical index values established by the NBS studies. Examination of the ship-fracture plates in which brittle fracture was initiated (SOURCE plates) had revealed that these were characterized by the absence of any visible evidences of ductility (deformation) generally expected of presumably ductile steels. Previous attempts to duplicate this performance in laboratory tests had not been successful because the notches used were of insufficient sharpness to produce fractures of essentially nil-ductility (pre-fracture ductility) at temperatures of service failures. The success of the crack-starter tests in this respect resulted from the development of a highly reproducible sharp, cleavage crack in the brittle weld deposited on the test plate.

Consequently, the drop-weight method of loading, which involved a relatively small testpiece containing the brittle weld, was developed and termed the drop-weight test. This test permitted laboratory determination of the temperature at which a given steel loses its ability to develop more than a minute amount of deformation in the presence of a sharp crack-like defect. The temperature at which this occurred was defined as the nil-ductility transition (NDT) temperature. The explosion tests likewise indicated the NDT temperature by the "flat break" fractures of the test data. Empirical results for ship-fracture steels[3] and statistical correlations with ship-fracture data (to be described) indicated that the NDT temperature of a steel represented the highest temperature for the initiation of brittle fractures by the given steel in conventional steel structures.

Following the development of the drop-weight test, an extensive evaluation program was conducted to establish the characteristic NDT temperatures for a wide variety of structural steels. Concomitantly, these steels were evaluated by use of conventional laboratory notch-bend tests (Charpy-V, keyhole, Schnadt, etc.). From the observed relationships[4, 5] of the respective transition curves for these specimens with the NDT, it was concluded that:

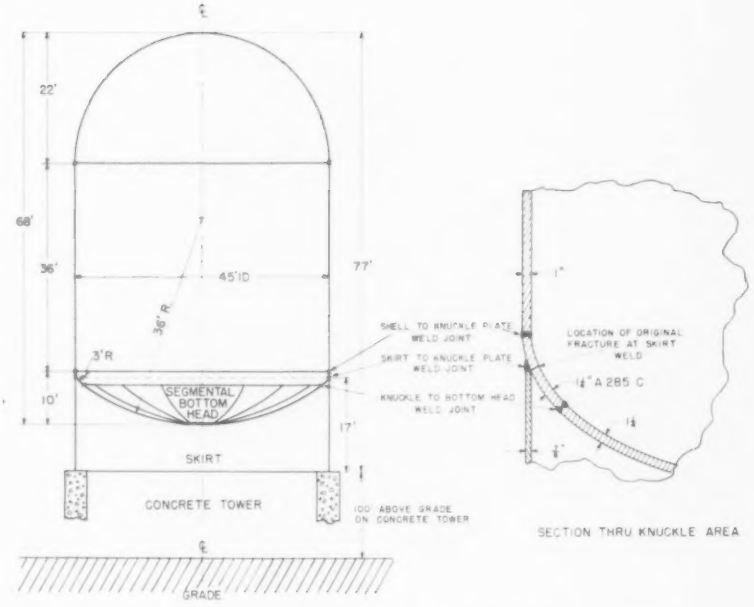


Fig. 7 Dimensional schematic of failed pressure vessel (left) and detail drawing of knuckle-plate area showing location of fracture origin

1. The Charpy-V specimen energy-transition curve offered the best possibility for general use as an index of the critical NDT temperatures.

2. A Charpy-V energy criterion determined to be proper for one type of steel could not be applied invariantly to all types of steels.

Other Charpy-V criteria such as energy fraction (percent of maximum energy), fracture appearance (percent shear), ductility (lateral contraction at the base of the notch), shear at the base of the notch and shear lips at the sides of the specimens were studied.[4, 5] However, none of these was found to be superior to the energy criterion, because these procedures likewise failed to indicate an invariant critical index for all types of steels. Because of the time

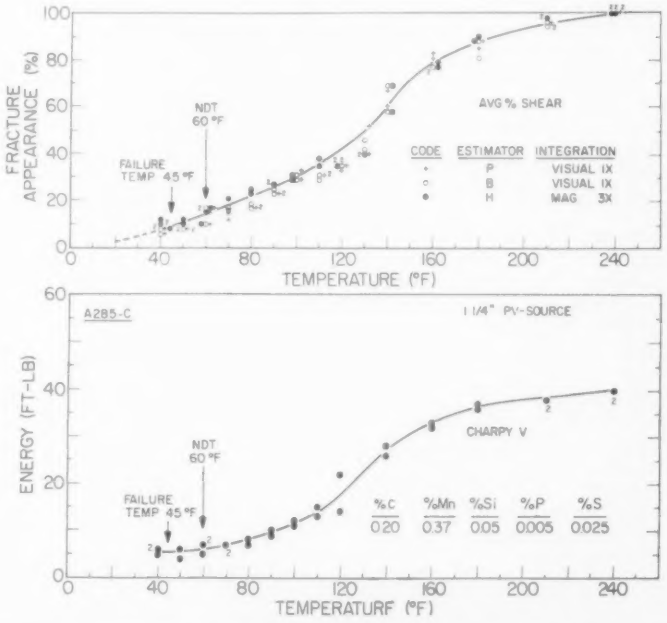


Fig. 8 Charpy-V-notch test data of Correlation Case No. 3 steel

Fig. 9 External appearance of crack in patched 20-yr-old pressure vessel (bottom), general view of cracked shell and patch plates (center), and enlarged view of fracture source area (top)

and the difficulties involved in making these determinations, as compared to establishing an energy curve, the studies which followed were generally restricted to Charpy-V energy curve correlations. Of the above criteria, only that of fracture appearance (percent shear) has received widespread attention by other investigators. It should be noted that conventional shear-fraction determinations are in fact estimations by individual observers and subject to individual bias as well as to statistical error. In attempts to reduce this variability, a method was developed[6] which makes use of a visual aid (grid system) whereby the shear fraction in the Charpy fracture surface is determined by an integration of squares.

As the result of the continuing series of crack-starter studies of conventional structural steels and low-alloy high-strength steels, including service-failure material, new engineering principles and design concepts were formulated by which the relative resistance to fracture of steels could be predicted from laboratory test data.[7,8] These concepts were aimed at an economical and practical specification of materials for specific service applications requiring assurance of protection from brittle fracture. The concepts were predicated upon the definition of a gradation of anticipated service stressing of the structure and the use of the critical fracture transition temperatures of the steels as determined in crack-starter tests.

It was proposed that conventional structures (merchant ships, pressure vessels, machinery components, etc.) should be considered to operate within elastic stress limits, with the exceptions of localized plastic hinge points (cutouts, corners, etc.) where small amounts of yielding would be expected to occur. This condition was described as the case of *General Service*. For this case, the *initiation* of brittle fracture would be considered possible only for the following conditions:

1. The metal temperature must be below the NDT temperature of the steel involved.
2. A crack notch must be present.
3. Loading must be such as to develop yielding in the region of the crack-notch.

Steel structures having NDT temperatures below that of the lowest anticipated service temperature could be expected to deform at these critical points without the initiation of brittle fracture, and would be considered immune to catastrophic failure.

Structures which have failed by brittle fracture in service may be considered as the *ultimate in "test" results* available to investigators of brittle-fracture phenomena. Obviously, complete and detailed investigations of all such service failures are essential for the development of proper preventive measures. Moreover, any concepts or guidelines which are proposed for the prevention of brittle fracture should be able to withstand the test of direct correlation with service performance.

This paper presents case histories and detailed data* developed in the investigation of a wide variety of service-failure steels.

## Correlations Established for Ship Fracture and World War II Type Steels

### *Correlation Case No. 1—1-in. World War II Ship Steel*

In December 1947, the T-2 tanker, the U.S.S. *Ponaganset*, broke in two

* Specific Charpy-V data for all specimens tested are furnished in the Appendix. Fracture appearance (percent shear) estimations were made in these studies using the visual graphical integration techniques previously described.[6] The average fracture appearance curves used for correlation purposes represent a single curve drawn for the estimations of three observers. In all of the correlation cases to be discussed except two (Correlation Cases 3 and 9) the material supplied for test purposes was known to be within a few feet or less of the source of failure.

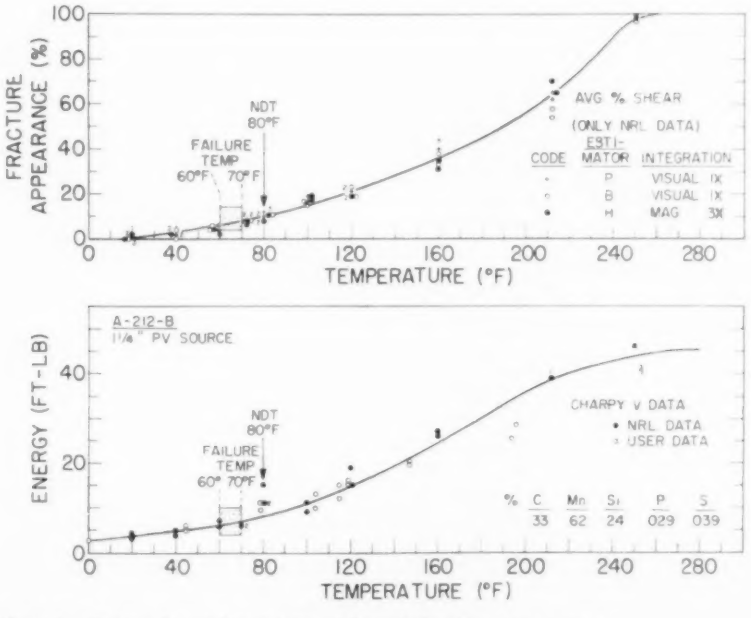


Fig. 10 Charpy-V-notch test data of Correlation Case No. 4 steel

while tied at dockside in Boston, Mass. The air temperature at the time of fracture was 35° F. The origin of failure was determined to be an arc strike made in the starboard deck plate where a chock and bracket was attached to the main deck, Fig. 1 top, and bottom left. Drop-weight specimens taken from the source plate material broke at 50° F and lower test temperatures but did not break at 60° F and higher test temperatures. The highest temperature at which one or more specimens break in the drop-weight test (50° F in this example) is defined as the NDT. Interpreted simply, the drop-weight test results indicate that at temperatures of 50° F and lower the source plate could not develop even minute plastic deformation in the presence of a cleavage crack. At 35° F, in the presence of an arc strike, *initiation* of brittle fracture developed in the ship in conformance with the predictions of the drop-weight tests. The average Charpy-V correlations, Fig. 2, indicate 8 ft-lb energy and 5% shear at the failure temperature (35° F) and 10 ft-lb energy and 9% shear at the NDT temperature (50° F). The fracture appearance data of Fig. 2 represent visual integration values made by only one estimator.

### *Correlation Case No. 2—⅝-in. World War II Ship Steel*

While moored to a buoy in Narragansett Bay in February 1954, the main deck and shell plating of the USS *Markab* fractured. A loud sound, described by a witness as similar to that generally made by the dropping of a heavy hatch cover, accompanied the development of the crack. As shown in Fig. 3, the crack originated from the corner of a square vent opening of an inboard main-deck plate (⅝ in. thick); propagated through a second plate (deck stringer, ¾ in. thick); and continued (Fig. 4) through one shell plate (shear strake, ⅝-in. thick). The shell plate cracks terminated in weld junction positions where the steel beams had been welded to the shell. The remedial field repairs, which were undertaken immediately after the failure, obliterated all evidences of the actual source point of failure. The weather conditions at the time of failure were reported to be wind—26 knots and air temperature—5° F.

Chemical analyses of the steels were as follows:

| | *C*, % | *Mn*, % | *Si*, % | *P*, % | *S*, % | *Cu*, % |
|---|---|---|---|---|---|---|
| ⅝-in. deck source plate | 0.17 | 0.40 | 0.05 | 0.026 | 0.031 | 0.06 |
| ¾-in. deck thru plate | 0.23 | 0.49 | 0.05 | 0.020 | 0.027 | 0.02 |
| ⅝-in. shell thru plate | 0.18 | 0.46 | 0.07 | 0.014 | 0.027 | 0.07 |

Macroetching and sulfur prints of samples from each steel involved in the failure indicated all three to be of the semikilled variety. The chemical compositions of these plates are considered to be typical of the medium-carbon ship-plate steel procured under either the Navy or the American Bureau of Shipping specifications used prior to 1948.

The Charpy-V correlations, Fig. 5, determined for the source plate displayed 5 ft-lb energy and 4% shear at the failure temperature (5° F) and 9 ft-lb energy and 12% shear at the NDT temperature (30° F).

**Table 1—Test Data for Ship Fracture and World War II Type Steels**

| | | | | | | | | | NDT | |
|---|---|---|---|---|---|---|---|---|---|---|
| | | | *Ship-fracture steels* | | | | | | | |
| *No.* | *Thickness, in.* | *Type** | *C*, % | *Mn*, % | *Si*, % | *P*, % | *S*, % | | °F | *Ft-lb* |
| 1 | ⅝ | R | 0.20 | 0.37 | 0.01 | 0.020 | 0.025 | | 0 | 4 |
| 2 | ⅝ | R | 0.21 | 0.36 | 0.01 | 0.030 | 0.028 | | 10 | 4 |
| 3 | ⅝ | R | 0.22 | 0.37 | 0.01 | 0.008 | 0.030 | | 10 | 3 |
| 4 | ⅝ | R | 0.20 | 0.34 | 0.01 | 0.008 | 0.030 | | 10 | 3 |
| 5 | ⅝ | SK | 0.31 | 0.49 | 0.10 | 0.012 | 0.037 | | 10 | 5 |
| 6 | ⅝ | SK | 0.25 | 0.36 | 0.04 | 0.020 | 0.028 | | 20 | 9 |
| 7 | ⅝ | R | 0.27 | 0.37 | 0.01 | 0.010 | 0.035 | | 20 | 5 |
| 8 | ⅝ | SK | 0.18 | 0.46 | 0.07 | 0.022 | 0.027 | | 20 | 8 |
| 9‡ | ⅝ | SK | 0.17 | 0.40 | 0.05 | 0.026 | 0.031 | | 30 | 9 |
| 10 | ¾ | R | 0.11 | 0.33 | 0.01 | 0.019 | 0.035 | | 0 | 5 |
| 11 | ¾ | R | 0.12 | 0.36 | 0.01 | 0.006 | 0.030 | | 10 | 5 |
| 12 | ¾ | R | 0.21 | 0.37 | 0.01 | 0.005 | 0.024 | | 20 | 4 |
| 13 | ¾ | SK | 0.29 | 0.42 | 0.07 | 0.010 | 0.039 | | 30 | 6 |
| 14 | ¾ | SK | 0.23 | 0.49 | 0.05 | 0.028 | 0.027 | | 40 | 8 |
| 15 | 13/16 | SK | 0.24 | 0.39 | 0.03 | 0.030 | 0.028 | | 30 | 5 |
| 16 | 13/16 | SK | 0.21 | 0.42 | 0.02 | 0.020 | 0.025 | | 30 | 7 |
| 17 | 1 | SK | 0.23 | 0.41 | 0.04 | 0.006 | 0.028 | | 40 | 6 |
| 18 | 1 | SK | 0.19 | 0.48 | 0.09 | 0.009 | 0.030 | | 40 | 9 |
| 19† | 1 | SK | 0.25 | 0.45 | 0.04 | 0.010 | 0.028 | | 50 | 10 |
| 20 | 1 | SK | 0.27 | 0.52 | 0.08 | 0.010 | 0.031 | | 60 | 10 |
| 21 | 1⅛ | R | 0.19 | 0.31 | 0.01 | 0.020 | 0.028 | | 50 | 7 |
| | | | *World War II Type Steels* | | | | | | | |
| 22 | ⅝ | SK | 0.19 | 0.38 | 0.01 | 0.007 | 0.021 | | 0 | 4 |
| 23 | ⅝ | SK | 0.25 | 0.49 | 0.01 | 0.010 | 0.033 | | 0 | 4 |
| 24 | ¾ | SK | 0.16 | 0.34 | 0.02 | 0.004 | 0.027 | | 0 | 5 |
| 25 | ¾ | SK | 0.23 | 0.48 | 0.02 | 0.009 | 0.039 | | 10 | 6 |
| 26 | ¾ | R | 0.24 | 0.45 | 0.02 | 0.013 | 0.043 | | 20 | 5 |
| 27 | ¾ | SK | 0.18 | 0.33 | 0.02 | 0.007 | 0.025 | | 20 | 5 |
| 28 | ¾ | SK | 0.25 | 0.50 | 0.05 | 0.012 | 0.036 | | 20 | 9 |
| 29 | ¾ | R | 0.27 | 0.41 | 0.01 | 0.037 | 0.045 | | 20 | 5 |
| 30 | ¾ | R | 0.25 | 0.35 | 0.02 | 0.008 | 0.040 | | 30 | 4 |
| 31 | ¾ | SK | 0.21 | 0.44 | 0.05 | 0.010 | 0.037 | | 30 | 5 |
| 32 | ¾ | SK | 0.24 | 0.48 | 0.05 | 0.014 | 0.033 | | 40 | 5 |
| 33 | 1 | SK | 0.19 | 0.49 | 0.05 | 0.010 | 0.017 | | 20 | 8 |
| 34 | 1 | SK | 0.24 | 0.45 | 0.05 | 0.004 | 0.024 | | 20 | 5 |
| 35 | 1 | SK | 0.22 | 0.46 | 0.08 | 0.010 | 0.030 | | 20 | 5 |
| 36 | 1 | SK | 0.17 | 0.49 | 0.09 | 0.007 | 0.030 | | 20 | 7 |
| 37 | 1 | SK | 0.21 | 0.54 | 0.03 | 0.013 | 0.038 | | 20 | 7 |
| 38 | 1 | SK | 0.20 | 0.51 | 0.04 | 0.013 | 0.026 | | 20 | 8 |
| 39 | 1 | SK | 0.23 | 0.46 | 0.05 | 0.009 | 0.034 | | 30 | 7 |
| 40 | 1 | R | 0.24 | 0.35 | 0.03 | 0.008 | 0.039 | | 30 | 4 |
| 41 | 1 | SK | 0.10 | 0.32 | 0.02 | 0.011 | 0.037 | | 30 | 9 |
| 42 | 1 | SK | 0.22 | 0.49 | 0.04 | 0.009 | 0.032 | | 40 | 10 |

*SK = Semikilled; R = Rimmed; confirmed by macroetch and sulfur print.
† Correlation Case No. 1.
‡ Correlation Case No. 2.

### Summary of NDT Relations for World War II Ship Fracture and Similar Steels

The vast number of records and reports describing investigations of the World War II ship-fracture steels represent the largest single body of fracture data available today for statistical correlations with service performance. A valid basis for the utilization of notch-bend specimen data for design purposes was provided by the excellent correlations of Charpy-V energy data obtained by the National Bureau of Standards[2] with ship fracture performance. Accordingly, new procedures and concepts devised for the prediction of service performance should be expected to give results for the ship plate

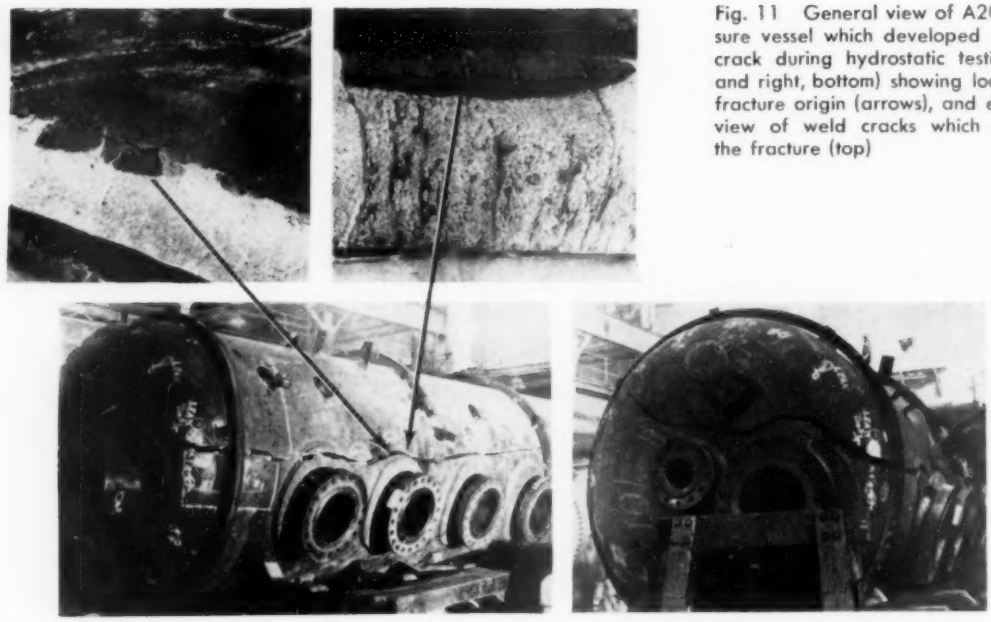

Fig. 11 General view of A204 pressure vessel which developed a brittle crack during hydrostatic testing (left and right, bottom) showing location of fracture origin (arrows), and enlarged view of weld cracks which initiated the fracture (top)

type steels which are in conformance with ship service history and, in addition, should correlate with Charpy-V data in an identical manner as that established by the NBS for ship-fracture plates. Table 1 summarizes NDT relationships for 42 rimmed and semikilled steels (21 of which were actually involved in ship fractures) tested at NRL. Based upon Charpy-V energy transition curves, these 42 steels are considered an excellent sampling[9] representative of approximately 90% of the ship-fracture steels tested by NBS. The range (3 to 10 ft-lb) and the average (6.2 ft-lb) correlations of Charpy-V energy with NDT are in excellent agreement with those reported for the failure temperatures of the ship-fracture source plates (range of 3 to 11 ft-lb and an average of 7.4 ft-lb).

The NDT data for these steels are depicted graphically in Fig. 6 (top). From the indicated distribution curve, the NDT predictions of potential initiation of fracture are indicated to start at approximately 60° F; and increasing possibility of fracture initiation is indicated as service operating temperatures are reduced to approximately 20 and 30° F. Actual ship service experience shown in Fig. 6 (bottom) indicates that, in fact, trouble-free service was encountered at 80° F and higher operating temperatures, but a prohibitive failure rate developed at operating temperatures of 30 to 40° F. The percentages shown on the bottom curve represent the probable fraction of NDT quality plates present at the respective temperatures. These latter data were taken from the curve shown in Fig. 6 (center) which was developed by correlation of NDT relationships with the NBS 15 ft-lb frequency-distribution curve of World War II ship plate. At temperatures of 70° F and higher, where 10% or less of the World War II ship plate were of NDT quality at the operating temperatures, the probability of expecting any or all of these plates to be at locations favorable to initiation of failure would, of course, be small. At 30° F, with one out of every two plates being of NDT quality, such probabilities would be very much increased and the prohibitive failure rate would be expected.

## Correlations Established for Non-Ship Structural-Service Failures

### *Case No. 3—1¼-in. A285 Grade C Pressure-Vessel Steel*

This failure represents the case of a

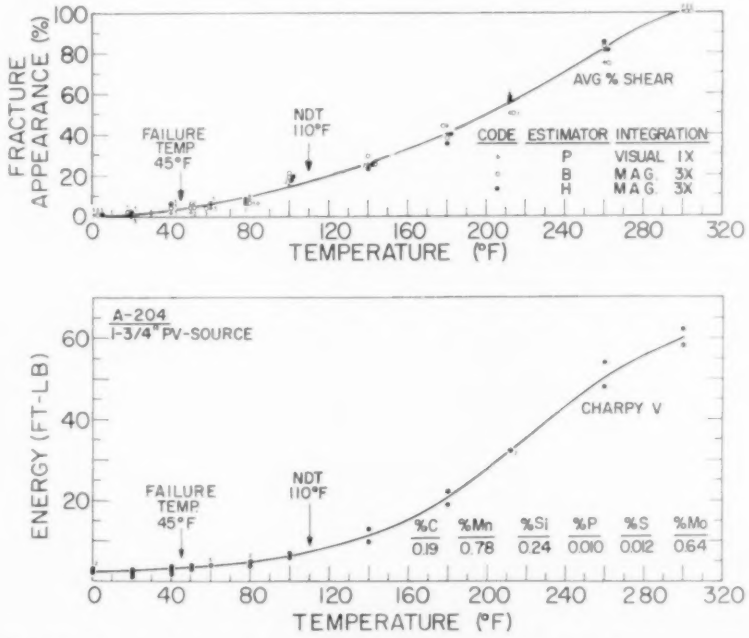


Fig. 12 Charpy-V-notch test data of Correlation Case No. 5 steel

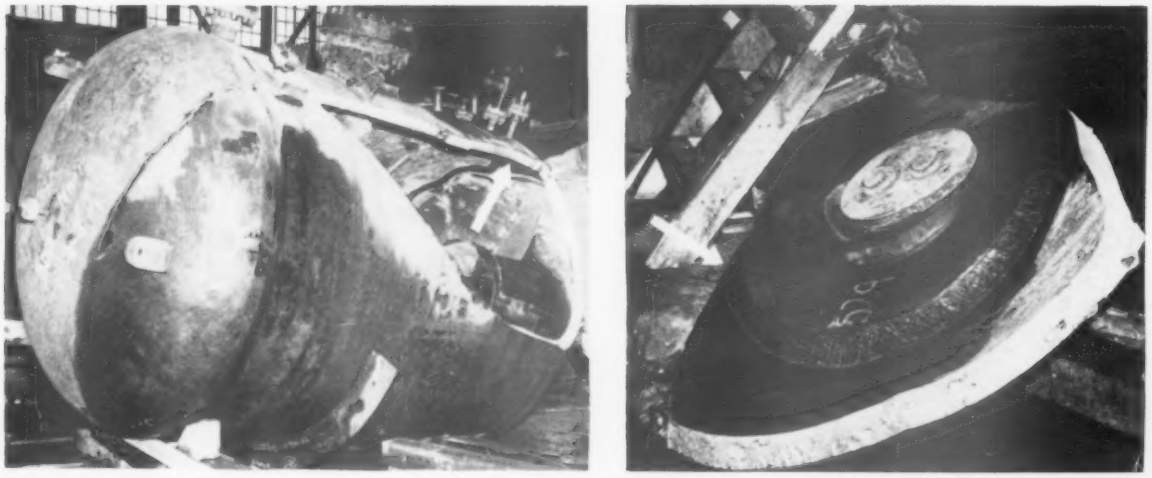

Fig. 13 Three-inch thick A302 pressure vessel which fractured during hydrostatic pressure testing. Arrows point to weld defect which served as source of fracture

hydrostatic-test failure of an unusually large pressure vessel constructed for petroleum-refinery operations. A photograph of the resulting damage is not available; however, schematic details of the vessel involved are shown in Fig. 7. During hydrostatic pressure testing with 45° F water at 60 psig (1½ times design pressures) brittle failure of the bottom head occurred approximately 45 min after the application of the overload pressure. Failure started in the knuckle plate at the root of the fillet weld which joined the supporting skirt plate to the bottom head. Fracture propagated circumferentially throughout the entire knuckle plate and fragmented (25 or more pieces) the bottom head. The fragmentation of the bottom head resulted in such an efflux of water as to develop a vacuum in the vertical pressure vessel which subsequently collapsed and crumpled.

The chemical composition of the failed A285 Grade C knuckle-plate was as follows:

| | |
|---|---|
| C, % | 0.20 |
| Mn, % | 0.37 |
| P, % | 0.005 |
| S, % | 0.025 |
| Si, % | 0.05 |
| Cu, % | 0.06 |

The average Charpy-V correlations, Fig. 8, displayed 5½ ft-lb energy and 9% shear at the failure temperature (45° F) and 6 ft-lb energy and 15% shear at the NDT temperature (60° F). This material was taken from the large plate in which the chevron pattern indicated the probable source. The fracture pattern was partially obliterated, however, and the engineers investigating this failure could not find a distinct flaw (trigger) from which this failure probably originated.

***Correlation Case No. 4—1¼-in. A212 Grade B Pressure-Vessel Steel***

A carbon-steel pressure vessel used in the chemical-processing industry had been in service for approximately 20 years. During this time, because of localized thinning of the vessel wall, it was repaired by welding. The vessel was repaired in conformance with Boiler Code procedures by insertion of small patch plates through the 16-in. ID manhole which were then welded to the internal surface of the vessel. In the area of patching, subsequent service corrosion extended small weld cracks into the shell of the vessel. In April 1955, the pressure vessel failed during a routine hydrostatic-pressure test. The vessel was located in a heated operating area and had been prepared for testing on the day prior to failure. At the time of failure (water temperature 60 to 70° F) a complete longitudinal rupture developed.

Although records were not available to indicate the steel type, subsequent chemical analysis revealed the material to be equivalent to that currently specified under ASTM A212 Grade B. Chemical composition and average mechanical properties determined for the failed plate were as follows:

| | |
|---|---|
| C, % | 0.33 |
| Mn, % | 0.62 |
| Si, % | 0.24 |
| P, % | 0.029 |
| S, % | 0.039 |
| Ni, % | 0.16 |
| Cr, % | 0.13 |
| Mo, % | 0.01 |

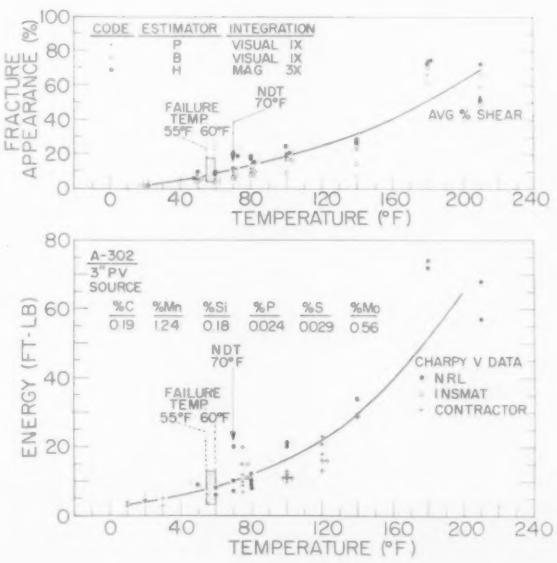


Fig. 14 Charpy-V-notch test data of Correlation Case No. 6 steel

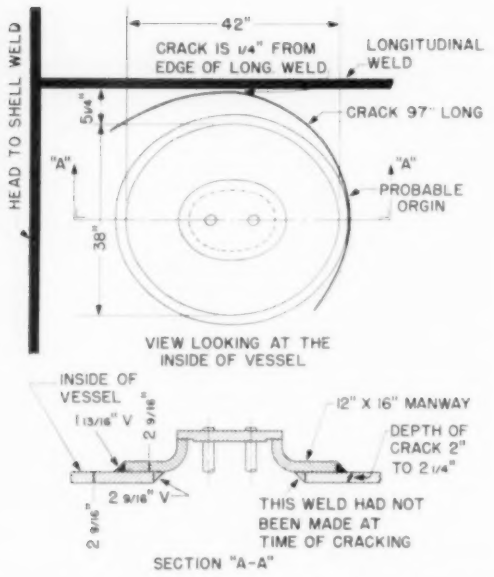


Fig. 15 Location and dimensions of crack which developed in A302 pressure vessel under action of residual stress

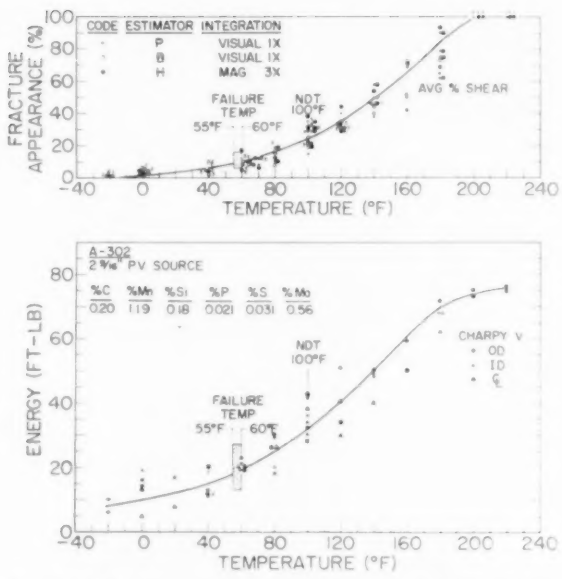


Fig. 16 Charpy-V-notch test data of Correlation Case No. 7 steel. Failure temperature correlates to 20 ft-lb energy level

| | |
|---|---|
| Cu, % | 0.14 |
| TS, (psi) | 77,400 |
| YS, (psi) | 37,900 |
| El. in 2 in., % | 25.5 |

Figure 9 depicts a portion of the vessel after failure and an enlarged view of the fracture-source area. Charpy-V correlations, Fig. 10, indicate excellent agreement between separate determinations made by the user and those made by NRL. At the failure temperature (60 to 70° F) the steel displayed 6 to 7 ft-lb Charpy-V energy and 6 to 8% shear; and at the NDT temperature (80° F) 8 ft-lb energy and 10% shear.

### *Correlation Case 5—1¾-in. A204 Pressure-Vessel Steel*

Because of enhanced resistance to graphitization and higher stress-rupture characteristics than that obtainable with plain-carbon steels, ASTM A204 Grade B steels are generally used for pressure vessels designed to operate at elevated temperatures (above about 750° F). For such service, notch toughness characteristics are seldom considered necessary. Pressure vessels fabricated completely within the requirements of the ASME Code, however, are subjected to hydrostatic proof-testing at normal water temperatures. At the elevated temperatures encountered in boiler service, it should be recognized that brittle fracture is not possible in these conventional structural carbon steels. However, if these materials do not possess adequate notch toughness at water temperatures, brittle failure may occur during hydrostatic

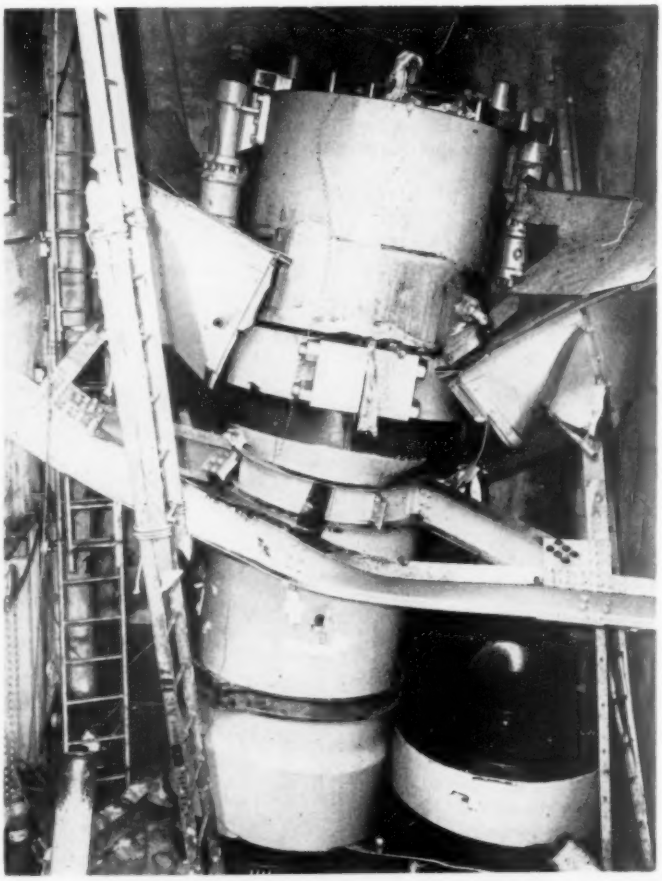

Fig. 17 Scene of 7⅝-in. thick A293 pressure-vessel failure which originated at a fatigue crack

Fig. 18 Bottom (failed) end of A293 pressure vessel. Source of fracture was fatigue crack at root of thread

testing as illustrated by the following example of a 7-ft diam pressure vessel fabricated for use as a CO converter. In compliance with code requirements, the top third of the vessel was constructed with A204 steel (900° F operation) and the bottom two thirds of the vessel with A212 steel (850° F operation). For improved corrosion resistance a $^1/_{16}$-in. Type 304 stainless-steel liner was plug-welded to the inner surface of the vessel. To avoid sensitizing the liner, the vessel was stress relieved at 950° F for 10 hr in accordance with Paragraph UCS-56 of the 1952 ASME Code. The major weld joints were X-rayed to meet the code requirements for weld quality.

In January 1956, while being hydrostatically tested with 45° F water at $1^1/_2$ times the design pressure, failure of the vessel occurred catastrophically. The rupture traversed the full length (both A204 and A212 sections) and one of the A204 heads, Fig. 11, left and right, bottom. The source of failure consisted of cracks (developed prior to stress relieving) which were located in the A-204 shell at the toe of the reinforcing-pad weld adjacent to the second manway, Fig. 11, top.

The chemical composition and average mechanical properties determined for the A204 source plate were as follows:

| | C, % | Mn, % | P, % | S, % | Si, % | Mo, % |
|---|---|---|---|---|---|---|
| A204 spec. | 0.23 max | 0.90 max | 0.035 max | 0.04 max | 0.15/0.30 | 0.40/0.60 |
| NRL | 0.19 | 0.78 | 0.010 | 0.012 | 0.24 | 0.64 |
| Mill | 0.19 | 0.82 | 0.006 | 0.008 | 0.23 | 0.56 |
| Fab. | 0.18 | 0.80 | 0.015 | 0.010 | 0.22 | 0.50 |

| | TS, psi | YS, psi | El. in 2 in., % | Bend test |
|---|---|---|---|---|
| A204 spec. | 70/85,000 | 40,000 min | 19.0 min | 180 deg |
| Mill (long) | 77,500 | 52,400 | 25.0 | OK |
| Fab. (long) | 83,000 | 55,500 | 38.0 | ... |
| Fab. (trans.) | 84,150 | 40,700 | 42.0 | ... |

Fig. 19 Full-section ($7^5/_8$-in. thick) drop-weight test specimen from body of heavy-section pressure-vessel failure (Fig. 17) broken at 60° F. Service temperature of pressure vessel was 70° F

The results of drop-weight and Charpy-V tests of the A204 source plate are shown in Fig. 12. At the failure temperature (45° F), the Charpy-V values averaged $3^1/_2$ ft-lb and 4% shear; and at the NDT temperature (110° F), 7 ft-lb and 17% shear.

Except when agreed upon between the manufacturer and purchaser, the ASTM Spec. A204–54T stipulates that plates 2 in. and under in thickness will be tested and supplied in the as-rolled condition, which was the condition of the $1^3/_4$-in. thick A204 source plate material.

Tests conducted for the A204 head and A212 shell sections through which fracture propagated gave the following results:

| | Thickness, in. | NDT, ° F | Average ft-lb at NDT | Average ft-lb at failure temperature |
|---|---|---|---|---|
| A-204 head | $1^3/_4$ | 80 | 13 | $7^1/_2$ |
| A-212 shell | $1^{13}/_{16}$ | 70 | 11 | $6^1/_2$ |

| | C, % | Mn, % | P, % | S, % | Si, % | Mo, % |
|---|---|---|---|---|---|---|
| A-204 head | 0.19 | 0.90 | 0.010 | 0.021 | 0.27 | 0.52 |
| A-212 shell | 0.27 | 0.76 | 0.013 | 0.023 | 0.20 | 0.02 |

These data have no relationship to source behavior but indicate that propagation of fracture is to be expected. Work at NRL[8] has shown that propagation of brittle fracture is possible in steels at temperatures approximately 40° F above their NDT temperatures. In this case, the temperature at failure was actually 45° F; i.e., considerably below the highest possible propagation temperatures of approximately 120° F for the A204 head (NDT 80° F) and 110° F for the A212 shell (NDT 70° F).

***Correlation Case No. 6—3-in. A302 Pressure-Vessel Steel***

Heavy section A302 material used for pressure vessels is conventionally required to be in the normalized condition. The following example of direct correlation of NDT with A302 service failures involved material which was not in the normalized condition. After the failure, it was ascertained that the 3-in. thick A302 shell plate inadvertently had received a 1650° F anneal (furnace cool) prior to the forming operations conducted at temperatures between 200 to 300° F. After complete fabrication, the vessel was stress relieved at 1150° F for 3 hr (heated from 350 to 1150° F at a rate of 200° F/hr and cooled to 600° F at a rate of 125° F/hr). In January 1954, this pressure vessel was subjected to hydrostatic proof testing (55 to 60° F water temperature). After a constant pressure of 2400 lb had been maintained for 22 min, failure occurred, Fig. 13. A one-ton, pear-shaped section of steel was thrown outward. The fracture was propagated through both hemispherical heads and almost completely through the other side of the vessel (not visible in Fig. 13). The source of failure (arrows) was a small, heat-affected-zone crack developed at the toe of the fillet weld between the manway reinforcing pad and the inside of the shell plate. The source crack was covered with oxide scale developed during the final stress relief of the pressure vessel, which suggests that it developed during fabrication and was not discovered by the conventional boiler-inspection procedures employed.

Chemical composition and average mechanical properties determined for the failed material were as follows:

| | |
|---|---|
| C, % | 0.19 |
| Mn, % | 1.24 |

| | |
|---|---|
| P, % | 0.024 |
| S, % | 0.029 |
| Si, % | 0.29 |
| Ni, % | 0.08 |
| Cr, % | 0.23 |
| Mo, % | 0.51 |
| TS (psi) | 68,000 |
| YS (psi) | 36,100 |
| El. (%2") | 52.3 |
| R.A. (%) | 46.8 |

Figure 14 compares Charpy-V data obtained by the Inspector of Naval Materials and the prime contractor with those obtained by NRL. The moderate scatter observed is believed to reflect the variability resulting from the heat-treated condition of this plate and the testing of different areas within this plate. The average Charpy-V correlations indicate 8 to 9 ft-lb energy and 7 to 9% shear at the failure temperature (55 to 60° F) and 10 ft-lb energy and 11% shear at the NDT temperature (70° F).

### *Correlation Case No. 7—2 9/16-in. A302 Pressure-Vessel Steel*

This case documents a service failure initiated at a temperature higher than the 10 ft-lb Charpy-V temperature of the steel involved. It concerns a 2 9/16-in. thick A302 pressure vessel which was identical in design to the one illustrated in Fig. 13. The longitudinal girth welds, one header-to-shell weld and the inside fillet weld at the edge of the manway saddle were completed when fabrication was interrupted to permit X-ray inspection of all welds. Minor weld repairs, using local preheats, were made on the longitudinal girth weld opposite the manway. The inspection and repair operations were completed during a 5-day period in October 1954 and the vessel was scheduled for loading into an intermediate stress-relieving furnace. During the early morning hours, while no work on this vessel was actually in progress, a loud shot-like noise was heard. The temperature at the time was approximately 55 to 60° F. Upon close examination of all the vessels within the shop, a 97-in. long crack was discovered in this vessel in the vicinity of the manway-saddle fillet weld, Fig. 15. The crack was not visible on the outside surface of the vessel, and instructions were given by the fabricator to determine the extent of the crack by grinding. Indications of the crack were still visible at various locations after grinding had proceeded to depths of 2 to 2 1/4 in. along the length of the crack.

The shell plate which cracked was known to have been mill-normalized (1790° F, air-cooled) and stress relieved by the fabricator (1150° F, 3-hr minimum holding with 130° F/hr maximum heating and cooling rate). From the similarity in appearance of the path of this failure to that shown for the previous A302 pressure-vessel failure, Fig. 13, it was assumed that the source of failure was a small weld crack or heat-affected-zone crack. Initiation of this failure resulted solely from residual stresses developed by the fillet welding of the manway saddle. The energy release was sufficient to propagate the failure more than 8 ft and essentially throughout the 2 9/16-in. thick plate. The fillet weld operation, however, had been made by the stringer-bead technique (designed to minimize residual stresses) with application of preheat (from the outside) to the whole manway saddle.

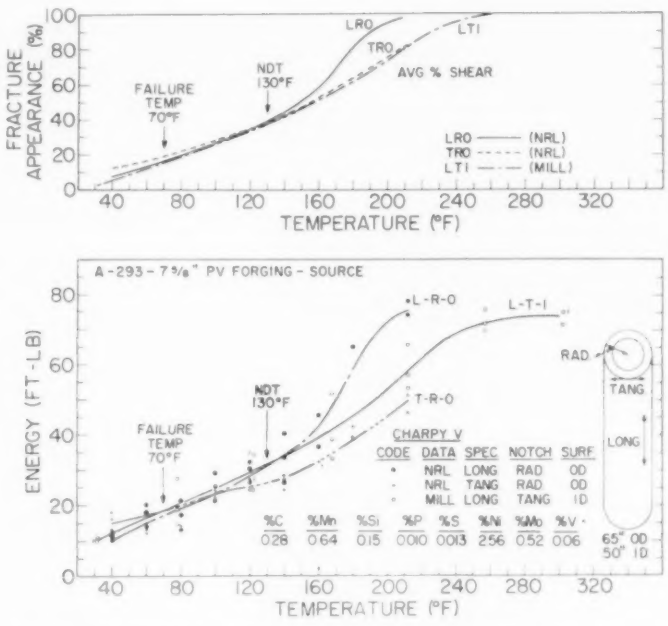


**Fig. 20 Charpy-V-notch test data of Correlation Case No. 8, showing correlation of failure temperature with 18 ft-lb energy level**

The chemical composition and average mechanical properties of the failed plate were as follows:

| | |
|---|---|
| C, % | 0.20 |
| Mn, % | 1.19 |
| P, % | 0.021 |
| S, % | 0.031 |
| Si, % | 0.18 |
| Ni, % | 0.22 |
| Cr, % | 0.12 |
| Mo, % | 0.56 |
| TS (psi) | 85,400 |
| YS (psi) | 63,300 |
| El. (%2") | 24.2 |
| R.A. (%) | 51.4 |

The elliptical manway cut-out section was removed immediately before starting the manway welding operations. Charpy-V and drop-weight tests were conducted with this cut-out material and the results are considered to be

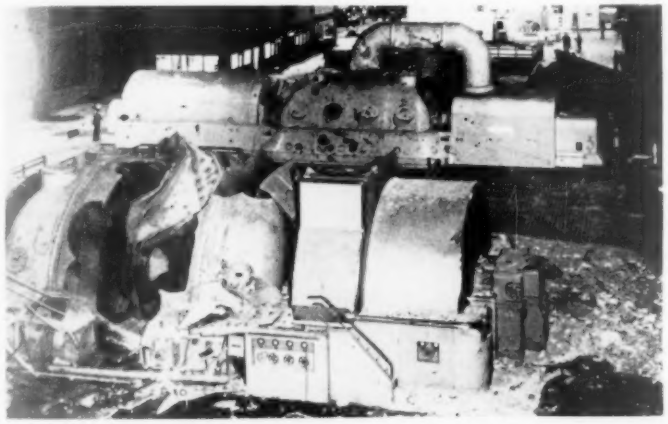

**Fig. 21 Scene of catastrophic failure of steam-turbine spindle indicating seriousness of such service failures. (Courtesy Allis-Chalmers Mfg. Co. and Trans. ASME)**

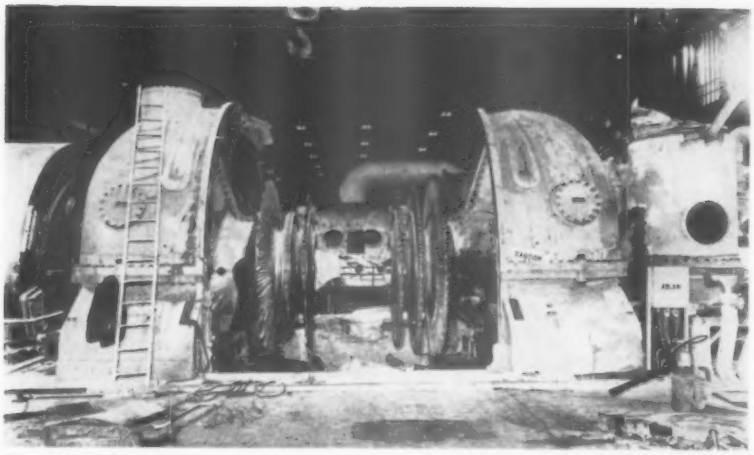

Fig. 22 Failed turbine showing stub of spindle shaft in which fracture initiated. Missing portion of shaft is the heavy central section (Fig. 23). (Courtesy Allis-Chalmers Mfg. Co. and Trans. ASME)

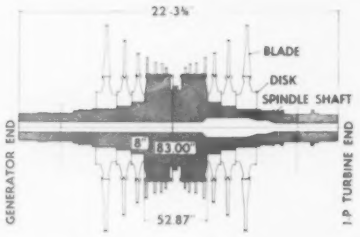


Fig. 23 Cross-sectional schematic of the failed turbine spindle. (Courtesy Allis-Chalmers Mfg. Co. and Trans. ASME)

representative of the properties characteristic to the fracture-source area. Figure 16 depicts NRL Charpy results for specimens machined from areas adjacent to both surfaces and from the centerline of the 2 $^{9}/_{16}$-in. thick steel. No significant differences were observed, so only one average transition curve is shown for these data. Of particular importance is the fact that Charpy-V tests conducted at the failure temperature (55 to 60° F) displayed an average energy absorption of 18 to 20 ft-lb and 9 to 10% shear. Charpy-V tests from other plate areas conducted by the steel mill, the fabricator, the prime contractor and the Inspector of Naval Materials agreed with NRL results in that all tests conducted at 60° F averaged at least 20 ft-lb. Charpy-V correlations at NDT temperature (100° F) averaged 32 ft-lb energy and 24% shear.

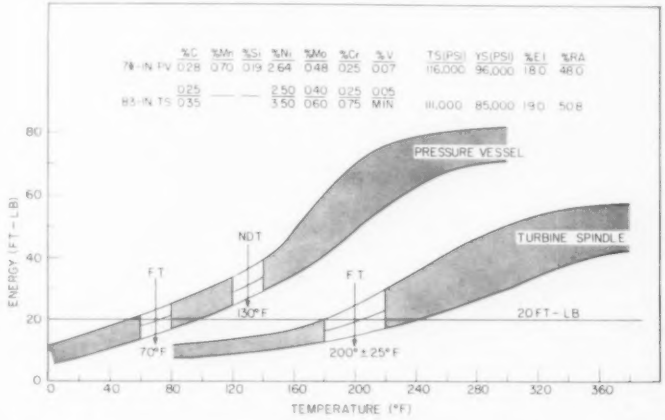
%C %Mn %Si %Ni %Mo %Cr %V TS(PSI) YS(PSI) %EL %RA
78-IN PV 0.28 0.70 0.19 2.64 0.48 0.25 0.07 116,000 96,000 18.0 48.0
83-IN TS 0.25 0.35 — — 2.50 3.50 0.40 0.60 0.25 0.75 0.05 MIN 111,000 85,000 19.0 50.8
PRESSURE VESSEL
TURBINE SPINDLE
NDT
F.T.
F.T.
150°F
70°F
200°±25°F
20 FT-LB
ENERGY (FT-LB)
TEMPERATURE (°F)

Fig. 24 Detailed test results for heavy-section service failures. Note Charpy-V relationships at failure temperatures (F.T.) indicate that both the pressure-vessel and turbine-spindle failures initiated at temperatures corresponding to Charpy-V 20 ft-lb transition temperatures

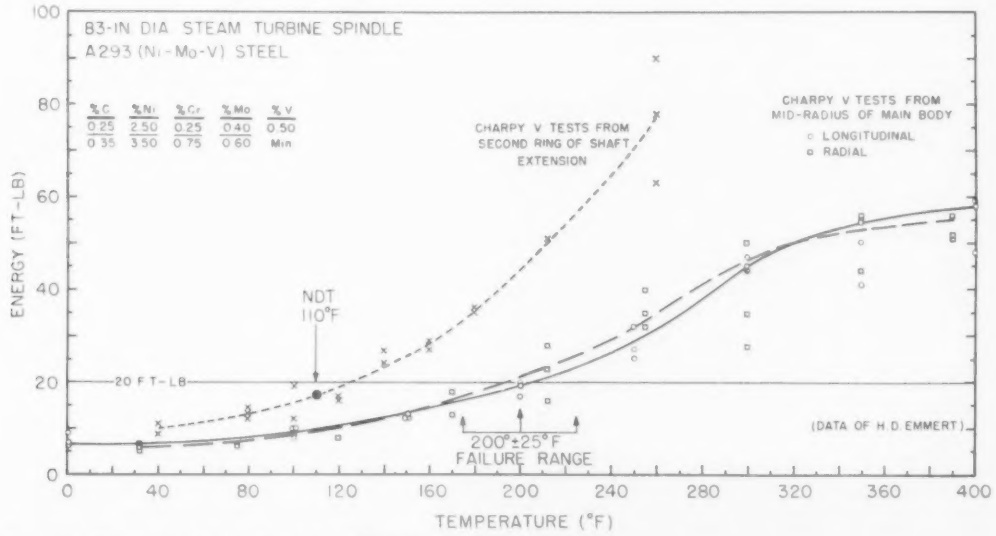


Fig. 25 Charpy-V-notch test data of Correlation Case No. 9 steel

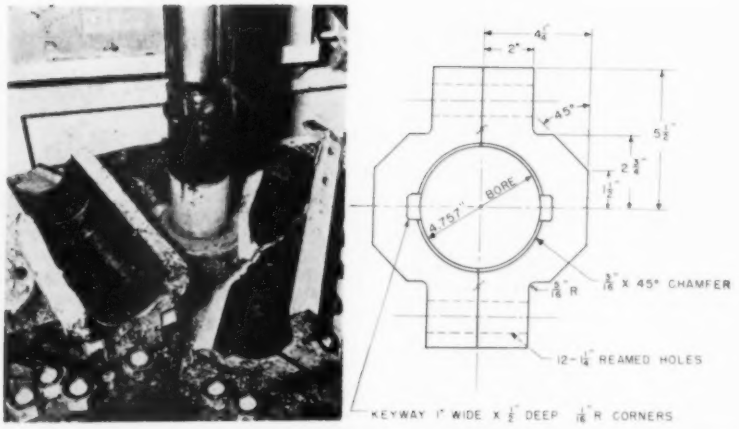


Fig. 26 Anchor windlass-shaft coupling after fracture through keyway sections during anchor-hoisting operations (left), and cross-sectional dimensions of coupling (right)

***Correlation Case No. 8—7⅝-in. A293 Ni-Mo-V Pressure-Vessel Forging***

Except in cases where the resulting damage has obliterated evidences of the source of failure, the initiation of numerous ship and nonship structural failures has always been traced to small crack defects. Even though extreme care may be used in welding fabrication, it is obviously difficult to eliminate all potential triggers in large welded structures. Questions have often been raised concerning the possibility of developing brittle failures which start from fatigue crack defects formed during service. The case of the failure in January 1956 of a heavy section (7⅝-in. thick A293 Ni-Mo-V steel) pressure-vessel forging, Fig. 17, presents an example of catastrophic brittle fracture which occurred after the formation of a small fatigue crack during service. The 100-ton pressure vessel was being used for pressurization cycles involving high-level stresses (30,000 psi). Both ends of the vessel contained threaded breech-block type closures. The vessel was operated only at room temperature (70° F), and loading and unloading operations resulted in completing approximately 3 or 4 pressure cycles a day. The high-level, cyclic stresses in conjunction with machine tool marks caused the development of small fatigue cracks at the root of the first thread in the bottom closure. The crack, caused by fatigue, was the initiator for the brittle failure which followed. At the time of failure, approximately 1550 pressure cycles had been completed. The brittle fracture completely severed the bottom head (Fig. 18) which cantilevered out and knocked the support legs from position, thus permitting the 100-ton weight to drop and collapse the superstructure. In addition to the complete circumferential fracture, the crack also propagated up throughout almost the entire length of the shell.

Material taken at the source area from below the circumferential fracture was tested in standard 1-in. thick specimen drop-weight tests and conventional Charpy tests. From the shell area, specimens measuring 6 x 60 in. x full thickness (7⅝ in.) were also supplied and tested in full thickness, Fig. 19. The NDT temperature for both sets of specimens was 130° F.* Chemical composition (A293) and average mechanical properties for the failure steel were as follows:

| | *C*, % | *Mn*, % | *P*, % | *S*, % | *Si*, % | *Ni*, % | *Mo*, % | *V*, % | *TS*, psi | *YS*, psi | *El*, % | *RA*, % |
|---|---|---|---|---|---|---|---|---|---|---|---|---|
| Mill | 0.28 | 0.70 | 0.015 | 0.010 | 0.19 | 2.64 | 0.48 | 0.07 | 116,000 | 96,000 | 18.0 | 48.0 |
| NRL | 0.28 | 0.64 | 0.010 | 0.013 | 0.15 | 2.56 | 0.52 | 0.06 | ... | ... | ... | ... |

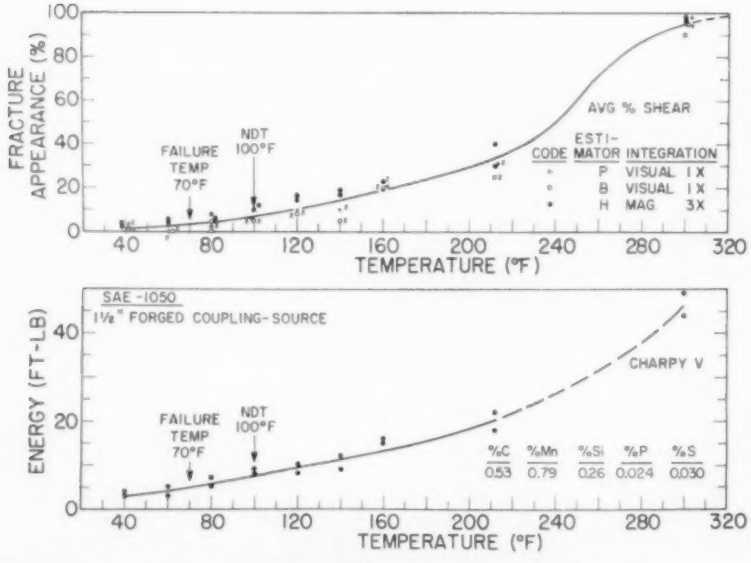


Fig. 27 Charpy-V-notch test data of Correlation Case No. 10 steel

The heat treatment for the failed outer shell consisted of double normalizing (1640° F for 12 hr, and 1550° F for 12 hr) followed by tempering (1160° F for 12 hr).

* Drop-weight tests are aimed at establishing fracture performance in steels at a technical level of ductility. A small (but finite) amount of plastic deformation, described as incipient surface yielding, is developed in the test. The use of a central anvil stop precludes the development of gross deformation. Normalization of testing procedures is required so as to facilitate the measurement of the same parameter (ductility in the presence of a notch) for all specimens regardless of variables involving specimen dimensions, strength of steel, etc. A normalization of procedures is accomplished by adjusting the drop weight, the height of the stop, or the span used for tests involving various specimens. Drop-weight tests of the above failure material were also conducted with specimens of the following sizes with conditions adjusted so as to develop equivalent surface strains in each specimen and results were as follows:

| *Specimen size, in.* | *NDT, ° F* |
|---|---|
| 1 x 3½ x 14 | 130 (represents standard test) |
| ¾ x 3½ x 14 | 110 |
| 1½ x 3½ x 14 | 140 |
| ½ x 2 x 5 | 110 |
| ⅝ x 2 x 5 | 120 |
| 6 x 7⅝ x 60 | 130 |

Fig. 28 Temper-embrittled ring forging which fractured after 40 yr of service as a result of electrical-arc strikes

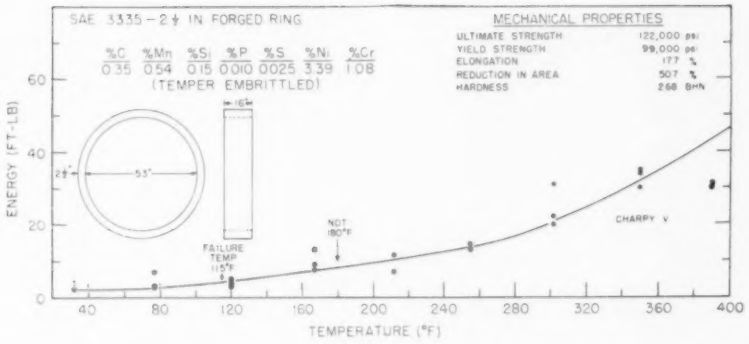


Fig. 29 Charpy-V-notch test data of Correlation Case No. 11 steel

From the material adjacent to the source of failure, Charpy-V specimens were machined and tested in two different orientations. Figure 20 indicates excellent agreement of results obtained in tests conducted in another orientation by the mill with those obtained by NRL. The fracture-appearance curves for NRL tests represent average curves of data by three observers as discussed previously. The data, which were omitted from this illustration for purposes of clarity, are supplied in the Appendix. At high temperatures and Charpy energy levels, the effect of specimen orientation is seen to be moderate. However, in the critical temperature range of interest (from NDT to failure temperature), no significant effect of orientation is apparent. Depending on orientation, the average Charpy-V correlations indicate 26 to 31 ft-lb energy and 37 to 39% shear at the NDT temperature (130° F). At the failure temperature (70° F) the average Charpy-V correlations indicate 17 to 18 ft-lb energy and 15 to 20% shear. Thus, these data for this steel (normalized-and-tempered, high-strength, fully-killed, low-alloy) document further evidence of the possibility of brittle fractures *initiating* at temperatures which correspond to those higher than the 10 ft-lb Charpy-V temperature. It should also be recognized that this pressure vessel was involved only in room-temperature service. The number of correlations discussed previously in which failure temperature was shown to be only 10 or 15° below the NDT temperature is considered sufficient to justify the belief that this failure still would have occurred at metal temperatures up to and including 130° F.

### *Correlation Case No. 9—83-in. A293 Ni-Mo-V Turbine-Spindle Steel*

In comparison with the test results shown for the heavy-section A293 Ni-Mo-V pressure-vessel forging failure, it is considered significant to review briefly the excellent data presented by Emmert[10] and Thum[11] of another A293 Ni-Mo-V steel failure. In December 1954, a large, low-pressure, steam-turbine spindle failed during a routine overspeed-trip test. The resulting damage is shown in Figs. 21 and 22 and a schematic drawing of the unit in Fig. 23. The spindle had been heat treated by double normalizing at 1640 and 1600° F and tempering at 1180° F.

The spindle failure originated at the main body core which was found to contain numerous flakes, some of which were approximately 4 in. in diam. It has been stated that the metal temperature at time of failure was estimated to be 200 ± 25° F (approximately 175° at the bore and 210 to 225° F at the circumference based on the prior 48-hr history and subsequent imposed steam temperatures). Charpy-V tests reported by Emmert[10] for specimens taken in two orientations from the midradius of the spindle main body indicate that this failure also initiated at a temperature corresponding to the average 20 ft-lb Charpy-V temperature. Figure 24 presents a summary of the pertinent data for this failure and that of the heavy-section pressure vessel. The shaded areas of both Charpy curves encompass the range of values developed at any one temperature for all specimens tested regardless of specimen orientation. It is obvious that both heavy-section failures initiated at temperatures corresponding to their average Charpy-V 20 ft-lb transition temperatures.

Material for drop-weight testing could not be obtained from the main body core of the turbine spindle because of the large flakes. NRL was furnished a 14- x 12- x 3½-in. block of steel which was cut from the second shaft extension of the failed spindle. Results of NRL Charpy and drop-weight tests are shown in Fig. 25. A higher notch toughness is indicated by these tests for the shaft

Fig. 30 General view of typical large-capacity aluminum-extrusion container. Note size of external retaining ring which failed under shrink stresses

Fig. 31 Cross section of fractured extrusion-container retaining ring (top) and close-up of origin area (arrows) at thermocouple attachment weld

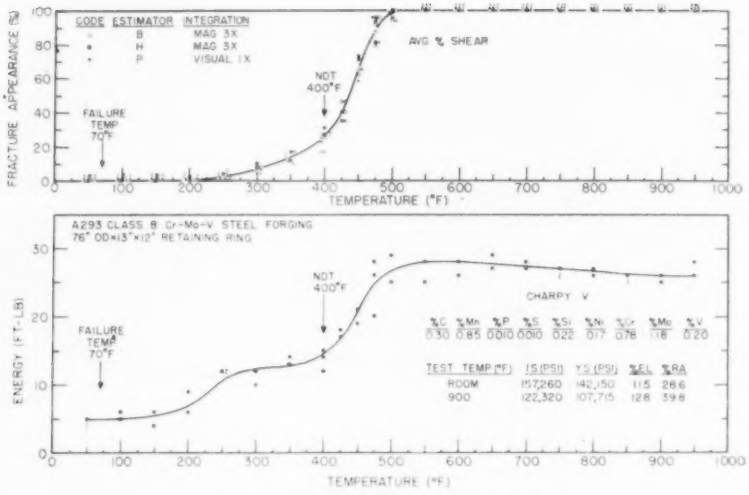
CODE ESTIMATOR INTEGRATION
B MAG 3X
H MAG 3X
P VISUAL 1X
AVG % SHEAR
FRACTURE APPEARANCE (%)
NDT 400°F
FAILURE TEMP 70°F
TEMPERATURE (°F)
A293 CLASS B Cr-Mo-V STEEL FORGING
76" OD x 13" x 12" RETAINING RING
CHARPY V
ENERGY (FT-LB)
%C %Mn %P %S %Si %Ni %Cr %Mo %V
0.30 0.85 0.010 0.010 0.22 0.17 0.78 1.18 0.20
TEST TEMP (°F) TS (PSI) YS (PSI) %EL %RA
ROOM 157,260 142,150 11.5 28.6
900 122,320 107,715 12.8 39.8

Fig. 32 Charpy-V-notch test and tensile-test data of Correlation Case No. 12 steel

extension than that shown to exist in the main body. In view of the section size differences, these data are explained by the metallurgy and heat treatment of the spindle involved. Of particular significance is the fact that the failure of the main body initiated at a metal temperature which correlates with the approximate 20 ft-lb Charpy-V temperature. Drop-weight tests of the shaft-extension material indicate a nil-ductility transition (NDT) temperature of 110° F which corresponds quite closely to the 20 ft-lb Charpy-V temperature of the shaft material. The orientation of Charpy specimens taken from the shaft extension could not be ascertained. On the basis of experience gained with other service-failure correlations, it is believed that drop-weight tests of material from the main body would indicate an NDT of 200° F or higher.

### *Correlation Case No. 10—1½-in. SAE 1050 Forged-Coupling Steel*

In May 1955, a forged-steel anchor-windlass shaft-coupling fractured in service aboard one of our U.S. Coast Guard vessels. The fracture occurred at a temperature of 70° F during a routine hoisting operation of the port anchor. The anchor was raised at slow (1/4) speed and was being housed at the moment of failure. The fracture resulted in splitting the coupling vertically through its entire length along the keyways, Fig. 26. Coupling pieces were thrown outward with sufficient force to damage oil piping lines, so it is possible that the anchor was housed under power. However, no circuit breakers were tripped to indicate excessive electrical loads.

The chemical composition of the failed coupling was determined to be as follows:

| | |
|---|---|
| C, % | 0.53 |
| Mn, % | 0.79 |
| Si, % | 0.26 |
| P, % | 0.024 |
| S, % | 0.030 |
| Ni, % | 0.23 |
| Cr, % | 0.10 |
| Mo, % | 0.06 |
| Cu, % | 0.21 |

Sufficient crack-free material was available for only one standard-size drop-weight specimen; however, by means of extensions welded to the halves of the initial test sample which broke at 100° F, it was possible to obtain additional tests at other temperatures. The respective Charpy-V correlations obtained, Fig. 27, were 5 ft-lb energy and 4% shear at the failure temperature (70° F) and 7½ ft-lb energy and 7% shear at the NDT temperature (100° F).

### *Correlation Case No. 11—58-in. OD x 2½-x 16-in. SAE 3335 Forged-Ring Steel*

The failure item represents a forged ring of old manufacture (approximately 1913) which had served for over 40 years as part of a high-speed rotating-machinery component. Early in 1955, the ring burst and caused considerable damage to surrounding equipment and facilities. The forged ring fractured into four quarter sections, Fig. 28

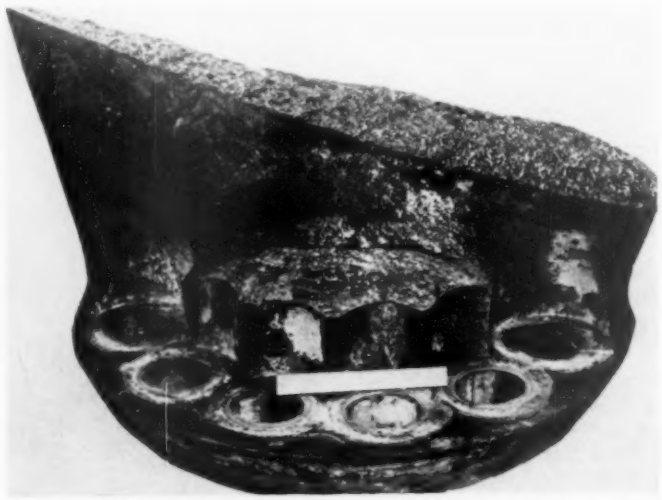

Fig. 33 Section of 12%-Cr stainless-steel ice-breaker propeller which failed in service by brittle fracture

Three of the four primary fracture paths passed through keyway fillets, and chevron markings clearly indicated that the failures started in the fillet corners. Visual examination of the failed section revealed cumulative heavy electrical arcing along the sides of a keyway. All data reported on this failure were obtained by an industrial laboratory equipped to run drop-weight tests. Metallographic examination revealed short disconnected cracks in the "arc-strike" area ascertained to be the original fracture source. At the time of failure, the metal-ring temperature was estimated to be approximately 115° F.

The chemical composition and average mechanical properties established for the failed ring were as follows:

| | |
|---|---|
| C, % | 0.35 |
| Mn, % | 0.54 |
| Si, % | 0.15 |
| P, % | 0.010 |
| S, % | 0.025 |
| Ni, % | 3.39 |
| Cr, % | 1.08 |
| TS, (psi.) | 122,000 |
| YS, (psi.) | 99,000 |
| El. in 2 in., % | 17.7 |
| R.A., (%) | 50.7 |
| Hardness, (BHN) | 268 |

The average Charpy-V correlations for the failed steel, Fig. 29, indicate 4½ ft-lb energy at the failure temperature (115° F) and 8½ ft-lb energy at NDT temperature (180° F). Fracture-appearance estimations were never made for this material, and, unfortunately, all specimens and surplus material have been discarded.

Of particular significance to this failure is the demonstrated fact concerning the requisite of a potential "trigger" as one of the conditions necessary and sufficient to develop brittle-fracture initiation. Laboratory studies of the failure steel revealed positive indications that the material was temper embrittled; i.e., evidence of an embrittling grain-boundary precipitate was clearly revealed by etching with a Zephiran chloride etching solution, and a "reversibility" of the embrittling action was indicated by the significant improvement in Charpy-V tests (without changing the strength properties) for failure material which was retempered at 1200° F and quenched in oil. During the 40-year service of the ring, however, it was known that the metal temperature never exceeded 160 to 170° F. Thus, it was deduced that the material could not have become temper-embrittled during its service. Since temper embrittlement was not recognized as a problem in 1913, it is probable that the heat treatment of the ring involved either furnace cooling or slow cooling in air after the tempering treatment. Although potentially capable of initiating failure during its entire life, over 40 years of service were obtained with this ring. Failure resulted only after the development of the crack-trigger caused by cumulative electrical arcing.

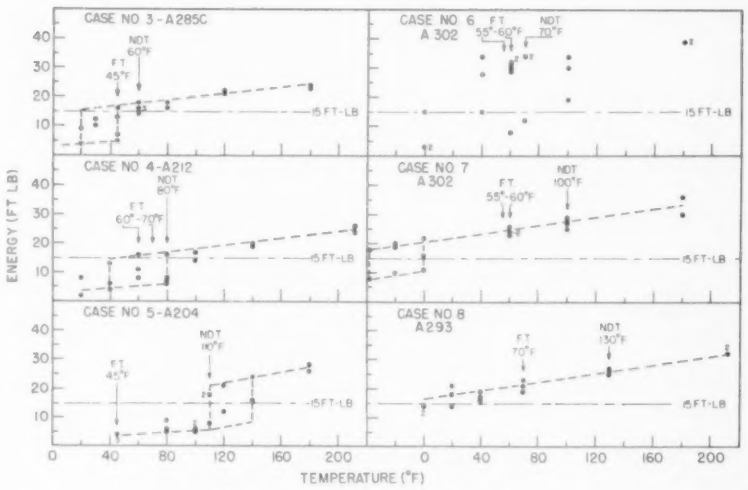


Fig. 34 Summary of Charpy-keyhole tests conducted on six pressure-vessel service-failure steels

### *Correlation Case No. 12—76-in. OD x 12-in. x 13-in. A293 Cr-Mo-V Retaining Ring for Aluminum-Extrusion Container*

This case represents the room-temperature failure of a large forging designed to operate at elevated temperatures of approximately 700 to 900° F. Large capacity extrusion presses have been built recently in order to supply the increased demands for heavier-complex extruded-aluminum forms. The general size and construction of the extrusion container assemblies used with these large presses are illustrated in Fig. 30. The container consists of an inner hot-work alloy liner, one shrink-fitted intermediate sleeve, and five shrink-fitted outer retaining rings (multiple-ring design). Normal working temperatures for extrusion operations are obtained by means of heating units inserted into the slots between the outer retaining rings. The design of these containers generally involved the scaling-up of smaller container designs and utilized available high-strength materials previously service-proved in smaller container service.

The steel in this container comforms to ASTM Standard A293-55T Class 8

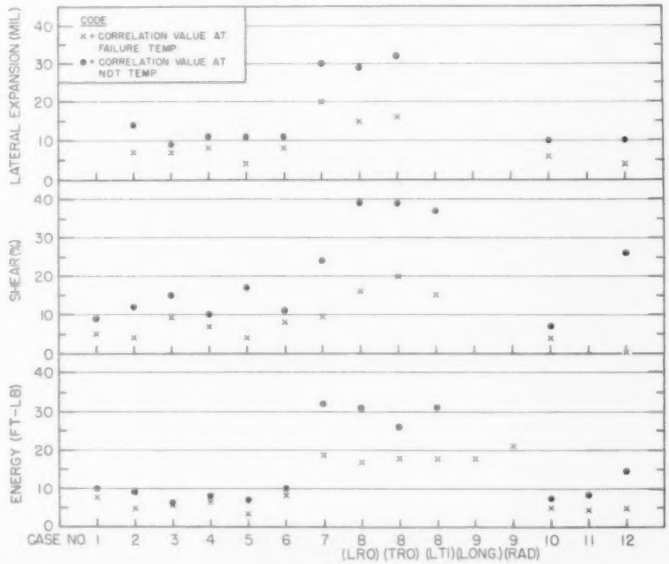
CODE
x = CORRELATION VALUE AT FAILURE TEMP.
● = CORRELATION VALUE AT NDT TEMP.
LATERAL EXPANSION (MIL)
SHEAR (%)
ENERGY (FT-LB)
CASE NO. 1 2 3 4 5 6 7 8 8 8 9 9 10 11 12
(LRO) (TRO) (LTI) (LONG) (RAD)

Fig. 35 Summary of Charpy-V correlations for service-failure steels

Table 2—Correlation Test Data for Brittle-Fracture Service Failures

| Case no. | Type | Failure: Temperature, °F | Failure: Charpy V, Ft-lb | Failure: Charpy V, Shear, % | Failure: Charpy V, Lat. exp., in. | NDT: Temperature, °F | NDT: Charpy V, Ft-lb | NDT: Charpy V, Shear, % | NDT: Charpy V, Lat. exp., in. |
|---|---|---|---|---|---|---|---|---|---|
| 1 | World War II ship | 35 | 8 | 5 | ... | 50 | 10 | 9 | ... |
| 2 | World War II ship | 5 | 5 | 4 | 0.007 | 30 | 9 | 12 | 0.014 |
| 3 | Pressure vessel | 45 | 5½ | 9 | 0.007 | 60 | 6 | 15 | 0.009 |
| 4 | Pressure vessel | 60/70 | 6–7 | 6–8 | 0.008 | 80 | 8 | 10 | 0.011 |
| 5 | Pressure vessel | 45 | 3½ | 4 | 0.004 | 110 | 7 | 17 | 0.011 |
| 6 | Pressure vessel | 55/60 | 8–9 | 7–9 | 0.008 | 70 | 10 | 11 | 0.011 |
| 7 | Pressure vessel | 55/60 | 18–20 | 9–10 | 0.020 | 100 | 32 | 24 | 0.030 |
| 8 (LRO) | Pressure vessel | 70 | 17 | 16 | 0.015 | 130 | 31 | 39 | 0.029 |
| 8 (TRO) | Pressure vessel | 70 | 18 | 20 | 0.016 | 130 | 26 | 39 | 0.032 |
| 8 (LT1) | Pressure vessel | 70 | 18 | 15 | ... | 130 | 31 | 37 | ... |
| 9 (Long) | Turbine spindle | 200 | 18 | .. | ... | ... | ... | .. | ... |
| 9 (Rad.) | Turbine spindle | 200 | 21 | .. | ... | ... | ... | .. | ... |
| 10 | Anchor windlass | 70 | 5 | 4 | 0.006 | 100 | 7½ | 7 | 0.010 |
| 11 | Forged ring | 115 | 4½ | .. | ... | 180 | 8½ | .. | ... |
| 12 | Retaining ring | 70 | 5 | 0 | 0.004 | 400 | 14½ | 26 | 0.010 |
| 13 | Propeller | 30/40 | ... | .. | ... | >210 | ... | .. | ... |
| 14 | Propeller | 30/40 | ... | .. | ... | >210 | ... | .. | ... |
| 15 | Propeller | 30/40 | ... | .. | ... | >210 | ... | .. | ... |
| 16 | Propeller | 30/40 | ... | .. | ... | >210 | ... | .. | ... |
| 17 | Propeller | 30/40 | ... | .. | ... | >210 | ... | .. | ... |

and had the following chemical composition and room-temperature mechanical properties:

| | |
|---|---|
| C, % | 0.30 |
| Mn, % | 0.85 |
| Si, % | 0.22 |
| P, % | 0.010 |
| S, % | 0.010 |
| Ni, % | 0.17 |
| Cr, % | 0.78 |
| Mo, % | 1.18 |
| V, % | 0.20 |

| Specimen No. | TS, psi | YS, psi | El. in 2 in., % | R.A., % | Hardness, BHN |
|---|---|---|---|---|---|
| 1 | 157,380 | 142,580 | 11.0 | 27.7 | 341 |
| 2 | 157,140 | 141,720 | 12.0 | 29.4 | 341 |

Because of contemplated modifications in the container design, it became necessary to obtain data concerning the actual metal temperatures reached in these outer retaining rings during normal extrusion operations. For this purpose, thermocouples were silver brazed at room temperature with local heating of an oxyacetylene torch to the back face of one of the outer retaining rings. After the brazing operations were completed, the container assembly was permitted to remain overnight at room temperature (70° F) with the intention of obtaining on the next day complete heating, operating and cooling thermal data under conditions of normal large-press production. The following morning in June 1957, however, with no work in progress and under only the residual stresses of the shrink-fit operation, a brittle fracture initiated and ruptured the entire 12- x 13-in. cross section of the ring, Fig. 31. The source of failure (arrows) was a cluster of small microcracks developed in the area of silver brazing.

The average Charpy-V correlations for the failed steel, Fig. 32, indicate 5 ft-lb energy and 0% shear at the failure temperature (70° F), and 14½ ft-lb energy and 26% shear at the NDT temperature (400° F).

### *Correlation Cases 13 to 17—12% Cr AISI Type 410 Stainless-Steel Cast Propellers*

The large, heavy-section propellers used for ice-breaker service are subjected to conditions of severe impact and to overloads which sometimes result in distortion or fracture of the blade sections. Various materials ranging from quenched-and-tempered high-strength carbon steels to nonferrous alloys (nickel-aluminum-bronze) have been used and have proved unsatisfactory because of material deficiencies and repair problems. The Type 410 stainless steel appeared to be a promising material for ship propellers because of economy, high strength and good corrosion resistance. During the early 1950's, however, frequent brittle failures were developed in the large, cast, Type 410 ice-breaker propellers, Fig. 33.

Sections from five similar ice-breaker propeller blades, all of which failed at ice-water temperatures (30 to 40° F), were investigated. In comparison with the specifications to which these blades were cast, the properties of these failed propellers were shown as in Table 3. The failed propellers obviously conformed to chemical requirements; however, the tensile properties were significantly below the minimum specification values.

Limited drop-weight tests were conducted for each as-received failure steel, and it was established that the NDT for each case was 210° F or higher (no tests conducted at higher temperatures). Having established the fact that each propeller failure had occurred at a temperature below the NDT temperature

Table 3

| | C, % | Mn, % | Si, % | Cr, % | Ni, % | Mo, % | S, % | P, % |
|---|---|---|---|---|---|---|---|---|
| Spec., max | 0.15 | 1.00 | 1.50 | 11.5/14.0 | 1.00 | 0.50 | 0.05 | 0.05 |
| Case No. 13 | 0.09 | 0.70 | 1.00 | 12.4 | 0.20 | 0.08 | 0.03 | 0.02 |
| Case No. 14 | 0.11 | 1.02 | 1.19 | 12.6 | 0.22 | 0.09 | 0.03 | 0.02 |
| Case No. 15 | 0.12 | 0.77 | 1.05 | 11.8 | 0.20 | 0.08 | 0.03 | 0.02 |
| Case No. 16 | 0.08 | 0.69 | 0.80 | 11.7 | 0.25 | 0.09 | .. | .. |
| Case No. 17 | 0.08 | 0.66 | 0.95 | 12.3 | 0.24 | 0.09 | .. | .. |

| | TS, psi | YS, psi | El. in 2 in., % | R.A., % | Hardness, BHN |
|---|---|---|---|---|---|
| Spec., min | 90,000 | 65,000 | 18.0 | 30.0 | .. |
| Case No. 13 | 56,000 | 39,000 | 5.0 | 5.0 | 161 |
| Case No. 14 | 75,000 | 45,000 | 17.0 | 21.0 | 174 |
| Case No. 15 | 55,000 | 37,000 | 5.0 | 5.0 | 156 |

of the steel involved, it was decided to use the remainder of the failure materials to investigate variables of heat treatment, section size, etc. From these studies,[12] it became apparent that for the large sections involved, specification changes in composition (silicon content lowered to 0.50% max, and molybdenum content changed to 0.50% min, plus the addition of approximately 0.75% Ni) and optimum heat-treating practices (austenitize 1750 to 1850° F, and temper at 1300 to 1350° F) would be required to provide material which may be expected to resist brittle fracture at ice-water temperatures.

### Relations to Charpy-Keyhole Transition Curves

Research investigators now agree that Charpy-V notch-impact tests are preferable for service-correlation purposes than are the keyhole tests. The latter, however, have long been accepted for specification purposes and some industries and code regulatory bodies have been reluctant to change. Despite laboratory-test evidence to the contrary,[4, 5, 7, 8] the 15 ft-lb keyhole provision of SA-300 ("Specification for Steel Plates for Pressure Vessels for Service at Low Temperatures") is sometimes popularly interpreted as adequate to provide absolute assurance of freedom from brittle fracture.

Results of keyhole tests conducted for the six pressure-vessel failure steels described previously (Correlation Cases 3 through 8, inclusive) are presented in Fig. 34 for information. The average curves have been drawn to show the characteristic shape (scatterband region) generally developed in keyhole transition curves. For correlation purposes, it obviously should be difficult to use such shallow gradually sloping curves as those shown in Fig. 34. The data shown for Case No. 6 illustrate the potential dangers of single-temperature keyhole tests and retest provisions of SA-300. Of the six keyhole tests conducted at 60° F (hydrostatic test failure temperature) five specimens gave values ranging from 29 to 32 ft-lb and one specimen developed only 8 ft-lb. Special significance is attached to the fact that the 15 ft-lb keyhole criterion does NOT provide assurance of safety; the data shown for Case Nos. 7 and 8 clearly indicate failures of pressure vessels at temperatures where the steels respectively developed 24 and 21 ft-lb average keyhole energy values.

## Discussion

A reliable validation of guidelines proposed for the prevention of brittle fracture of steel structures must be predicated on direct correlations with a substantial number of service failures bracketing a wide variety of steels. Previous studies of ship-fracture type steels demonstrated conclusively that *initiation* of brittle fracture was possible only at the NDT temperature or below. This finding has now also been validated for the case of nonship structures. Drop-weight correlations with standard notch-bend type specimens, cross correlations with the NBS ship fracture studies and correlations for nonship cases indicate that conventional Charpy-V specimens may be utilized for practical design and specification purposes.

Charpy-V energy, fracture appearance and lateral contraction criteria do not correlate to the NDT temperature at invariant index levels for all types of steels. Table 2 and Fig. 35 present a summary of Charpy-V energy, fracture appearance and lateral-expansion* correlations for the service-failure source steels described herein. These data indicate the following:

1. In all cases, the temperature of service failure was below the NDT temperature of the steel involved. For six cases, the failure temperature was only 10 to 30° F below the respective NDT temperature.

2. Energy correlations are not invariant. In three cases, failure *initiation* occurred at temperatures which corresponded to approximately the 20 ft-lb Charpy-V temperatures. The Charpy-V energy values for the various failure steels ranged from 4 to 20 ft-lb at the respective failure temperatures and from 6 to 32 ft-lb at the respective NDT temperatures.

3. Fracture appearance correlations are not invariant. The percent shear values ranged from 0 to 20% at the respective failure temperatures and from 7 to 39% at the respective NDT temperatures.

4. Lateral-expansion correlations are not invariant. Lateral-expansion values ranged from 4 to 20 mils (approximately 1.0 to 5.1% of width) at the respective failure temperatures and from 9 to 30 mils (approximately 2.3 to 7.6% of width) at the respective NDT temperatures.

* Lateral-expansion measurements have been suggested by Gross and Stout[13] as a criterion of notch-ductility which *might* correlate at an invariant level for all steel types.

Based on the information presented in this report and other supporting data detailed in the various references, it is concluded that:

1. Charpy-V specifications aimed at the prevention of brittle fracture should be based upon demonstrated specific correlations to NDT for the various steel types.

2. The energy criterion of the Charpy-V test provides the most significant and practical method for the notch-ductility indexing of steels.

3. The procedures developed for welded-plate structures are applicable to the case of forgings and castings.

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## APPENDIX: "CHARPY - V TEST DATA"

### Correlation Case No. 1—1-in. World War II Ship Steel

| Temperature, °F | Energy, ft-lb | Fracture appearance, % shear* Est. P 1X visual |
|---|---|---|
| −20 | 4 | 0 |
| −20 | 3 | 0 |
| 0 | 5 | 0 |
| 0 | 4 | 0 |
| 10 | 5 | 6 |
| 10 | 5 | 6 |
| 20 | 6 | 2 |
| 20 | 6 | 2 |
| 30 | 6 | 6 |
| 30 | 8 | 8 |
| 35 | 8 | 2 |
| 35 | 8 | 4 |
| 50 | 10 | 8 |
| 50 | 11 | 10 |
| 60 | 13 | 15 |
| 60 | 14 | 10 |
| 70 | 12 | 15 |
| 70 | 30 | 21 |
| 80 | 34 | 25 |
| 80 | 18 | 21 |
| 80 | 30 | 15 |
| 100 | 26 | 31 |
| 100 | 37 | 35 |
| 100 | 47 | 40 |
| 100 | 48 | 35 |
| 120 | 69 | 56 |
| 120 | 74 | 60 |
| 140 | 75 | 81 |
| 140 | 75 | 73 |
| 160 | 77 | 85 |
| 160 | 89 | 90 |
| 180 | 74 | 90 |
| 180 | 92 | 95 |
| 210 | 89 | 98 |
| 210 | 92 | 98 |

* Estimation of fracture appearance was made by only one observer. Lateral-expansion measurements could not be made because specimens were discarded.

### Correlation Case No. 4—1¼-in. A212 Grade B Pressure-Vessel Steel

| Temperature, °F | Energy, ft-lb | Fracture appearance, % shear— Est. P 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| 20 | 3 | 0 | 0 | 0 | 2 |
| 20 | 4 | 0 | 0 | 2 | 3 |
| 40 | 4 | 2 | 0 | 2 | 3 |
| 40 | 5 | 2 | 4 | 2 | 6 |
| 60 | 6 | 6 | 4 | 4 | 8 |
| 60 | 7 | 4 | 6 | 2 | 8 |
| 70 | 6 | 8 | 8 | 6 | 9 |
| 70 | 6 | 8 | 8 | 8 | 7 |
| 80 | 11 | 10 | 10 | 10 | 14 |
| 80 | 11 | 12 | 10 | 8 | 12 |
| 80 | 15 | 10 | 10 | 8 | 17 |
| 100 | 9 | 19 | 17 | 19 | 12 |
| 100 | 11 | 17 | 15 | 17 | 13 |
| 120 | 15 | 21 | 19 | 19 | 20 |
| 120 | 19 | 21 | 23 | 19 | 22 |
| 160 | 26 | 40 | 33 | 31 | 33 |
| 160 | 27 | 44 | 38 | 35 | 31 |
| 212 | 39 | 62 | 58 | 65 | 43 |
| 212 | 39 | 65 | 54 | 70 | 45 |
| 250 | 46 | 98 | 97 | 99 | 59 |

### Correlation Case No. 2—⅝-in. World War II Ship Steel

| Temperature, °F | Energy, ft-lb | Fracture appearance, % shear— Est. P 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral Expansion, mil |
|---|---|---|---|---|---|
| −20 | 3 | 0 | 2 | 0 | 3 |
| −20 | 4 | 0 | 2 | 0 | 4 |
| 0 | 4 | 2 | 4 | 2 | 5 |
| 0 | 6 | 2 | 4 | 2 | 7 |
| 20 | 6 | 6 | 8 | 6 | 6 |
| 20 | 6 | 6 | 8 | 6 | 7 |
| 20 | 6 | 8 | 8 | 6 | 10 |
| 20 | 7 | 6 | 8 | 8 | 11 |
| 20 | 8 | 10 | 8 | 6 | 11 |
| 40 | 13 | 23 | 25 | 25 | 18 |
| 40 | 14 | 21 | 20 | 23 | 18 |
| 40 | 17 | 17 | 15 | 21 | 22 |
| 60 | 21 | 29 | 29 | 29 | 24 |
| 60 | 21 | 29 | 33 | 29 | 28 |
| 60 | 22 | 33 | 42 | 33 | 29 |
| 60 | 23 | 25 | 23 | 27 | 32 |
| 60 | 24 | 31 | 27 | 27 | 31 |
| 80 | 36 | 52 | 46 | 42 | 39 |
| 80 | 38 | 38 | 50 | 44 | 43 |
| 80 | 47 | 58 | 54 | 46 | 53 |
| 80 | 52 | 50 | 58 | 50 | 52 |
| 100 | 58 | 77 | 69 | 75 | 62 |
| 100 | 62 | 79 | 75 | 81 | 63 |
| 120 | 67 | 81 | 77 | 81 | 73 |
| 120 | 72 | 81 | 83 | 85 | 72 |
| 120 | 73 | 94 | 94 | 98 | 77 |
| 140 | 76 | 96 | 96 | 98 | 75 |
| 140 | 77 | 96 | 96 | 98 | 75 |
| 160 | 79 | 98 | 98 | 98 | 80 |
| 160 | 80 | 98 | 98 | 98 | 82 |
| 180 | 78 | 100 | 100 | 100 | 75 |
| 180 | 78 | 100 | 100 | 100 | 77 |

### Correlation Case No. 3—1¼-in. A285 Grade C Pressure-Vessel Steel

| Temperature, °F | Energy, ft-lb | Fracture appearance, % shear— Est. P 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| 40 | 5 | 6 | 8 | 10 | 5 |
| 40 | 6 | 8 | 6 | 12 | 8 |
| 40 | 6 | 8 | 8 | 8 | 7 |
| 50 | 4 | 8 | 8 | 10 | 7 |
| 50 | 6 | 8 | 8 | 12 | 6 |
| 60 | 5 | 10 | 10 | 10 | 7 |
| 60 | 7 | 15 | 17 | 17 | 11 |
| 60 | 7 | 17 | 17 | 15 | 9 |
| 70 | 7 | 12 | 17 | 21 | 10 |
| 70 | 7 | 15 | 16 | 21 | 10 |
| 80 | 7 | 17 | 17 | 23 | 11 |
| 80 | 8 | 17 | 19 | 25 | 13 |
| 90 | 9 | 23 | 25 | 27 | 15 |
| 90 | 10 | 23 | 23 | 27 | 16 |
| 100 | 11 | 29 | 31 | 31 | 17 |
| 100 | 12 | 33 | 29 | 29 | 18 |
| 110 | 13 | 31 | 31 | 35 | 22 |
| 110 | 15 | 31 | 33 | 38 | 21 |
| 120 | 14 | 35 | 35 | 35 | 22 |
| 120 | 22 | 33 | 35 | 35 | 31 |
| 130 | 18 | 40 | 42 | 40 | 27 |
| 130 | 21 | 52 | 46 | 40 | 31 |
| 140 | 26 | 60 | 58 | 58 | 36 |
| 140 | 28 | 67 | 69 | 69 | 36 |
| 160 | 32 | 81 | 77 | 77 | 45 |
| 160 | 33 | 83 | 77 | 79 | 46 |
| 180 | 36 | 85 | 81 | 88 | 48 |
| 180 | 37 | 88 | 88 | 90 | 51 |
| 210 | 38 | 96 | 94 | 98 | 55 |
| 210 | 38 | 96 | 96 | 98 | 51 |
| 240 | 40 | 100 | 100 | 100 | 54 |
| 240 | 40 | 100 | 100 | 100 | 59 |

## Correlation Case No. 5—1¾-in. A204 Pressure-Vessel Steel

| Temperature, °F | Energy, ft-lb | Fracture appearance, % shear — Est. P 1X visual | Est. B 3X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| 0 | 2 | 0 | 0 | 0 | 2 |
| 0 | 3 | 0 | 0 | 0 | 2 |
| 0 | 3 | 0 | 0 | 0 | 2 |
| 20 | 1 | 0 | 2 | 2 | 3 |
| 20 | 2 | 0 | 2 | 2 | 2 |
| 20 | 3 | 0 | 2 | 2 | 3 |
| 40 | 2 | 2 | 4 | 6 | 3 |
| 40 | 3 | 2 | 4 | 6 | 4 |
| 40 | 4 | 2 | 4 | 6 | 4 |
| 50 | 3 | 2 | 4 | 4 | 4 |
| 50 | 4 | 2 | 4 | 4 | 4 |
| 60 | 9 | 4 | 4 | 6 | 4 |
| 60 | 10 | 4 | 4 | 6 | 5 |
| 60 | 11 | 4 | 4 | 6 | 5 |
| 80 | 4 | 8 | 6 | 6 | 6 |
| 80 | 5 | 6 | 6 | 6 | 7 |
| 80 | 5 | 10 | 6 | 8 | 7 |
| 100 | 6 | 15 | 19 | 17 | 8 |
| 100 | 7 | 15 | 21 | 19 | 10 |
| 140 | 10 | 25 | 25 | 23 | 16 |
| 140 | 13 | 25 | 29 | 25 | 17 |
| 180 | 19 | 44 | 44 | 40 | 24 |
| 180 | 22 | 40 | 38 | 35 | 29 |
| 212 | 32 | 50 | 50 | 56 | 40 |
| 212 | 32 | 60 | 50 | 58 | 39 |
| 260 | 48 | 81 | 83 | 85 | 53 |
| 260 | 54 | 75 | 75 | 81 | 56 |
| 300 | 58 | 100 | 100 | 100 | 68 |
| 300 | 62 | 100 | 100 | 100 | 71 |

## Correlation Case No. 6—3-in. A302 Pressure-Vessel Steel

| Temperature, °F | Energy, ft-lb | Fracture appearance, % shear — Est. P 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| 20 | 4 | 0 | 2 | 2 | 2 |
| 20 | 4 | 0 | 2 | 2 | 3 |
| 50 | 9 | 6 | 4 | 6 | 10 |
| 50 | 9 | 8 | 6 | 10 | 9 |
| 60 | 6 | 6 | 4 | 8 | 8 |
| 60 | 8 | 6 | 4 | 10 | 9 |
| 70 | 7 | 8 | 6 | 21 | 8 |
| 70 | 10 | 10 | 6 | 19 | 13 |
| 70 | 20 | 12 | 8 | 12 | 22 |
| 80 | 8 | 12 | 8 | 15 | 10 |
| 80 | 9 | 10 | 10 | 17 | 9 |
| 80 | 10 | 12 | 8 | 19 | 12 |
| 80 | 12 | 15 | 8 | 15 | 14 |
| 100 | 11 | 17 | 10 | 21 | 13 |
| 100 | 20 | 21 | 19 | 25 | 22 |
| 100 | 21 | 21 | 17 | 21 | 23 |
| 140 | 29 | 23 | 15 | 27 | 32 |
| 140 | 34 | 23 | 25 | 29 | 37 |
| 180 | 72 | 67 | 62 | 75 | 69 |
| 180 | 74 | 75 | 71 | 73 | 64 |
| 210 | 57 | 54 | 50 | 52 | 52 |
| 210 | 68 | 60 | 67 | 73 | 65 |

## Correlation Case No. 7—2 9/16-in. A302 Pressure-Vessel Steel

| Temperature, °F | Energy, ft-lb | Fracture appearance, % shear — Est. P 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| | | *Location centerline* | | | |
| 0 | 5 | 4 | 4 | 4 | 7 |
| 0 | 13 | .. | .. | .. | .. |
| 20 | 8 | .. | .. | .. | .. |
| 20 | 17 | .. | .. | .. | .. |
| 40 | 12 | .. | .. | .. | .. |
| 40 | 12 | .. | .. | .. | .. |
| 60 | 20 | .. | .. | 8 | 21 |
| 60 | 23 | 12 | 10 | 10 | 24 |
| 80 | 26 | 19 | 17 | 12 | 27 |
| 80 | 18 | .. | .. | .. | .. |
| 100 | 32 | 27 | 21 | 21 | 32 |
| 100 | 38 | 35 | 29 | 29 | 36 |
| 120 | 30 | 27 | 33 | 31 | 28 |
| 120 | 51 | 40 | 35 | 44 | 45 |
| 140 | 50 | 46 | 44 | 46 | 42 |
| 140 | 40 | 38 | 40 | 46 | 38 |
| 180 | 62 | 62 | 62 | 69 | 60 |
| 180 | 72 | 65 | 73 | 79 | 65 |
| | | *Location ID* | | | |
| −20 | 6 | 0 | 0 | 0 | 8 |
| −20 | 10 | 0 | 0 | 0 | 9 |
| 0 | 19 | 2 | 2 | 6 | 16 |
| 40 | 13 | 4 | 2 | 4 | 13 |
| 40 | 10 | 4 | 2 | 4 | 12 |
| 60 | 19 | 12 | 8 | 8 | 19 |
| 60 | 21 | 12 | 8 | 10 | 21 |
| 80 | 20 | 10 | 8 | 10 | 16 |
| 80 | 29 | 19 | 15 | 19 | 27 |
| 100 | 30 | 29 | 21 | 31 | 30 |
| 100 | 34 | 35 | 25 | 35 | 35 |
| 100 | 36 | 35 | 21 | 38 | 36 |
| 140 | 48 | 50 | 46 | 54 | 40 |
| 140 | 49 | 58 | 50 | 58 | 48 |
| 180 | 68 | 79 | 75 | 75 | 51 |
| 180 | 68 | 90 | 90 | 94 | 53 |
| | | *Location OD* | | | |
| 0 | 14 | 4 | 6 | 2 | 16 |
| 0 | 16 | 4 | 6 | 2 | 16 |
| 40 | 20 | 6 | 10 | 4 | 19 |
| 40 | 20 | 6 | 10 | 4 | 20 |
| 60 | 19 | 8 | 10 | 4 | 18 |
| 60 | 20 | 8 | 10 | 4 | 21 |
| 80 | 26 | 8 | 12 | 6 | 25 |
| 80 | 30 | 12 | 12 | 12 | 30 |
| 100 | 28 | 15 | 19 | 19 | 29 |
| 100 | 32 | 23 | 21 | 21 | 32 |
| 120 | 34 | 33 | 31 | 29 | 35 |
| 120 | 40 | 33 | 31 | 31 | 40 |
| 160 | 50 | 50 | 52 | 42 | 41 |
| 160 | 59 | 69 | 73 | 71 | 55 |
| 200 | 73 | 100 | 100 | 100 | 69 |
| 200 | 75 | 100 | 100 | 100 | 70 |
| 220 | 75 | 100 | 100 | 100 | 61 |
| 220 | 76 | 100 | 100 | 100 | 66 |

(Specimens for which no fracture appearance data are shown were used for heat-treatment studies of A302 material.)

### Correlation Case No. 8—7⅝-in. A293 Ni-Mo-V Pressure-Vessel Forging

| Temperature, °F | Energy, ft-lb | —Fracture appearance, % shear— Est. P 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| | | *LRO orientation* | | | |
| 40 | 10 | 8 | 12 | 8 | 6 |
| 40 | 11 | 8 | 10 | 10 | 9 |
| 40 | 12 | 4 | 4 | 12 | 10 |
| 60 | 14 | 12 | 12 | 15 | 14 |
| 60 | 18 | 12 | 10 | 17 | 16 |
| 60 | 20 | 12 | 10 | 15 | 17 |
| 80 | 13 | 17 | 15 | 23 | 7 |
| 80 | 17 | 17 | 15 | 21 | 15 |
| 80 | 21 | 17 | 17 | 21 | 18 |
| 100 | 21 | 21 | 17 | 29 | 20 |
| 100 | 25 | 29 | 29 | 31 | 25 |
| 100 | 29 | (Specimen used for micro—not available for shear or lateral-expansion numbers) | | | |
| 120 | 26 | 33 | 33 | 40 | 27 |
| 120 | 30 | 31 | 31 | 38 | 28 |
| 120 | 32 | 35 | 31 | 35 | 26 |
| 140 | 26 | 35 | 40 | 46 | 27 |
| 140 | 33 | 42 | 54 | 54 | 32 |
| 140 | 40 | 29 | 40 | 42 | 32 |
| 160 | 36 | 56 | 52 | 50 | 34 |
| 160 | 45 | 50 | 58 | 52 | 42 |
| 180 | 65 | 85 | 88 | 81 | 56 |
| 212 | 74 | 98 | 98 | 98 | 64 |
| 212 | 78 | 100 | 100 | 100 | 68 |
| | | *TRO orientation* | | | |
| 40 | 13 | 8 | 10 | 10 | 9 |
| 40 | 16 | 17 | 20 | 17 | 10 |
| 40 | 18 | 10 | 15 | 12 | 10 |
| 60 | 12 | 15 | 20 | 15 | 14 |
| 60 | 13 | (Not available for shear) | | | |
| 60 | 18 | 15 | 20 | 12 | 18 |
| 80 | 14 | 17 | 25 | 23 | 10 |
| 80 | 17 | 17 | 25 | 17 | 18 |
| 80 | 21 | 21 | 30 | 19 | 20 |
| 100 | 22 | 29 | 35 | 29 | 20 |
| 100 | 24 | 33 | 35 | 31 | 25 |
| 120 | 24 | 25 | 35 | 38 | 27 |
| 120 | 27 | 31 | 35 | 33 | 28 |
| 120 | 27 | 35 | 40 | 38 | 31 |
| 140 | 24 | 33 | 45 | 44 | 29 |
| 140 | 27 | 40 | 40 | 40 | 36 |
| 140 | 28 | 35 | 35 | 42 | 36 |
| 160 | 32 | 46 | 50 | 58 | 42 |
| 160 | 32 | 52 | 55 | 52 | 38 |
| 160 | 32 | (Not available for shear) | | | |
| 180 | 39 | 73 | 75 | 65 | 57 |
| 180 | 42 | 75 | 75 | 60 | 57 |
| 212 | 46 | 77 | 85 | 73 | 65 |
| 212 | 51 | 88 | 85 | 71 | 70 |

### Correlation Case No. 9—83-in. A293 Ni-Mo-V Turbine-Spindle Steel

| Temperature, °F* | Energy, ft-lb | —Fracture appearance, % shear— Est. P. 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| 40 | 9 | 2 | 4 | 6 | 8 |
| 40 | 11 | 2 | 6 | 8 | 6 |
| 80 | 12 | 6 | 8 | 8 | 10 |
| 80 | 13 | 8 | 8 | 12 | 10 |
| 80 | 14 | 8 | 10 | 10 | 12 |
| 100 | 12 | 8 | 19 | 21 | 10 |
| 100 | 19 | 10 | 19 | 19 | 16 |
| 120 | 16 | 19 | 23 | 25 | 14 |
| 120 | 17 | 17 | 23 | 23 | 14 |
| 140 | 24 | 19 | 23 | 23 | 18 |
| 140 | 27 | 19 | 21 | 17 | 20 |
| 160 | 27 | 29 | 27 | 29 | 23 |
| 160 | 29 | 33 | 35 | 31 | 25 |
| 180 | 35 | 46 | 35 | 38 | 30 |
| 180 | 36 | 42 | 33 | 44 | 32 |
| 212 | 51 | 58 | 50 | 52 | 42 |
| 260 | 63 | 81 | 79 | 88 | 55 |
| 260 | 78 | 85 | 85 | 92 | 64 |
| 260 | 90 | 100 | 96 | 100 | 74 |

*NRL tested specimens; cut from second ring of shaft extension.

### Correlation Case No. 10—1½-in. SAE 1050 Forged-Coupling Steel

| Temperature, °F | Energy, ft-lb | —Fracture appearance, % shear— Est. P 1X visual | Est. B 1X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| 40 | 3 | 0 | 0 | 2 | 3 |
| 40 | 4 | 0 | 0 | 4 | 4 |
| 60 | 3 | 0 | 0 | 4 | 2 |
| 60 | 5 | 0 | 0 | 6 | 5 |
| 80 | 5 | 2 | 0 | 8 | 6 |
| 80 | 7 | 2 | 5 | 6 | 6 |
| 100 | 8 | 6 | 5 | 10 | 10 |
| 100 | 9 | 6 | 5 | 12 | 11 |
| 120 | 8 | 8 | 10 | 17 | 11 |
| 120 | 10 | 8 | 10 | 15 | 12 |
| 140 | 9 | 10 | 5 | 19 | 11 |
| 140 | 12 | 10 | 5 | 17 | 16 |
| 160 | 15 | 19 | 20 | 23 | 17 |
| 160 | 16 | 21 | 20 | 23 | 19 |
| 212 | 18 | 31 | 25 | 30 | 21 |
| 212 | 22 | 31 | 25 | 40 | 24 |
| 300 | 44 | 98 | 95 | 98 | 47 |
| 300 | 49 | 94 | 90 | 96 | 52 |

### Correlation Case No. 12—76-in. OD x 12- x 13-in. A293 Cr-Mo-V Retaining Ring for Aluminum-Extrusion Container

| Temperature, °F | Energy, ft-lb | —Fracture appearance, % shear— Est. P 1X visual | Est. B 3X visual | Est. H 3X visual | Lateral expansion, mil |
|---|---|---|---|---|---|
| 50 | 5 | 0 | 0 | 0 | 3 |
| 50 | 5 | 0 | 0 | 0 | 4 |
| 100 | 5 | 0 | 0 | 0 | 6 |
| 100 | 6 | 0 | 0 | 0 | 5 |
| 150 | 4 | 0 | 0 | 0 | 2 |
| 150 | 6 | 0 | 0 | 0 | 4 |
| 200 | 6 | 0 | 0 | 0 | 3 |
| 200 | 9 | 0 | 0 | 2 | 8 |
| 250 | 12 | 2 | 1 | 4 | 5 |
| 250 | 12 | 2 | 0 | 4 | 6 |
| 300 | 10 | 4 | 8 | 10 | 7 |
| 300 | 12 | 4 | 6 | 8 | 10 |
| 350 | 13 | 17 | 12 | 17 | 8 |
| 350 | 14 | 10 | 12 | 12 | 8 |
| 400 | 12 | 17 | 17 | 27 | 7 |
| 400 | 14 | 25 | 25 | 27 | 10 |
| 400 | 15 | 23 | 27 | 31 | 10 |
| 425 | 17 | 35 | 40 | 35 | 14 |
| 425 | 18 | 46 | 46 | 40 | 13 |
| 450 | 19 | 58 | 62 | 71 | 14 |
| 450 | 21 | 65 | 65 | 73 | 17 |
| 475 | 20 | 81 | 79 | 81 | 15 |
| 475 | 26 | 88 | 92 | 94 | 17 |
| 475 | 28 | 96 | 96 | 96 | 18 |
| 500 | 25 | 94 | 94 | 96 | 20 |
| 500 | 29 | 99 | 100 | 100 | 19 |
| 550 | 25 | 100 | 100 | 100 | 17 |
| 550 | 28 | 100 | 100 | 100 | 20 |
| 600 | 26 | 100 | 100 | 100 | 20 |
| 600 | 28 | 100 | 100 | 100 | 18 |
| 650 | 27 | 100 | 100 | 100 | 20 |
| 650 | 29 | 100 | 100 | 100 | 21 |
| 700 | 27 | 100 | 100 | 100 | 22 |
| 700 | 28 | 100 | 100 | 100 | 20 |
| 750 | 27 | 100 | 100 | 100 | 18 |
| 750 | 27 | 100 | 100 | 100 | 18 |
| 800 | 26 | 100 | 100 | 100 | 19 |
| 800 | 27 | 100 | 100 | 100 | 19 |
| 850 | 26 | 100 | 100 | 100 | 16 |
| 850 | 26 | 100 | 100 | 100 | 18 |
| 900 | 25 | 100 | 100 | 100 | 17 |
| 900 | 26 | 100 | 100 | 100 | 17 |
| 950 | 26 | 100 | 100 | 100 | 18 |
| 950 | 28 | 100 | 100 | 100 | 20 |

# DESIGN VALUES FOR THERMAL STRESS IN DUCTILE MATERIALS

*Methods are proposed for the application of strain-cycling data to practical design problems involving thermal stress*

BY B. F. LANGER

ABSTRACT. A fundamental difference between thermal and mechanical stresses is that yielding produces relaxation of the thermal stress. It, therefore, follows that the methods used for safety evaluation of load stresses should not be applied to the calculated values of thermal stress. In this paper methods are proposed for the application of strain-cycling data to practical design problems involving thermal stress. The subjects considered include multiaxial stress conditions, combined steady and alternating stress, and cumulative damage. It is shown that when the calculated thermal stress exceeds the yield stress of a ductile material, some of the commonly used concepts, such as the Soderberg Diagram, must be modified. The design methods proposed constitute safe approximations which are consistent with the experimental data available.

## Introduction

### Difference Between Thermal Stress and Load Stress

When a member is loaded by mechanical means, such as weight or pressure, to a stress beyond its yield strength, the yielding will continue until the member breaks, unless strain hardening or redistribution of stresses limits the deflection. The internal stress must be in equilibrium with the external load, and since the external load is fixed the internal stress cannot relax. On the other hand, when a member is loaded by thermal means, such as differential expansion, yielding produces relaxation of the thermal stress. Differential expansion requires only that a prescribed *strain* pattern be satisfied, and the *stress* pattern need only satisfy the requirement for equilibrium of internal forces. This difference between thermal and load stresses is of fundamental importance and the criteria used for evaluating the safety of load stresses should not be applied to the calculated values of thermal stress.

There are some examples of stresses produced by mechanically applied loads that have the same characteristics as thermal stress and should, therefore, be evaluated by the same methods. Such are the cases in which a light structure is connected to a comparatively heavy structure, and the heavy structure is loaded by mechanical means. The magnitude of the distortion is completely controlled by the heavy structure. The stress in the light structure is determined by the strain in the heavy structure, and if the light structure yields, its stress relaxes. A good example is a pressure vessel supported by a cylindrical skirt. When the vessel is pressurized, the skirt must accommodate itself to the increased vessel diameter, but it takes little or no part in resisting the pressure. Therefore, a weak, ductile material might be quite satisfactory for the skirt, even though a high-strength material is required for the vessel.

## Application of Strain-Cycling Data

Except for extreme cases, a single application of thermal stress in sound, ductile material does not cause fracture. (It may, of course, produce unacceptable distortions.) It can also be shown that static thermal strains in a structure do not affect its load-carrying capacity (collapse load). Fractures produced by thermal stress are the result of repetitive cycling. In order to evaluate the possibility of fracture from thermal cycling, it is necessary to have data for the material which give the cycles-to-failure vs. the strain range. When this information is available it may be compared to the calculated thermal strains in the member. Since most such formulas express *stress* as a function of temperature distribution, assuming elastic behavior of the material, it is convenient to transform the test data on the material from the form of *strain* vs. cycles-to-failure to the form of *stress* vs. cycles-to-failure by multiplying the strain values by the elastic modulus of the material. The resulting values have the dimensions of stress but, since the tests were made in the plastic range, they do not represent actual stresses.

Another possible procedure leaves the test results in the form of strain instead of stress. It is important to note that when this method is used the significant strain is the one obtained by dividing the calculated stress by the modulus of elasticity, and not the strain obtained by applying Hooke's Law to the calculated stress. Of course, for the case of uniaxial stress there is no difference. When the stress is biaxial, however, the strain calculated from the equations of Hooke's Law does not include that necessary to cancel out the lateral contraction from the perpendicular strain. For example, consider the case of biaxial tension that is produced in a plate which is restrained at the edges when its temperature is reduced $T°$ F. If $\alpha$ is the expansion coefficient and $\nu$ is Poisson's ratio, the principal strains are $\epsilon_1 = \epsilon_2 = \alpha T$ and the principal stresses are $\sigma_1 = \sigma_2 = E\alpha T/1-\nu$. In order to achieve the net strain $\epsilon_1$ the metal must be mechanically strained not only by the amount $\epsilon_1$ but also by the additional amount required to overcome the lateral contraction produced by $\epsilon_2$. Therefore, the strain we are interested in is $\alpha T/1-\nu$, not $\alpha T$.

When strains exceed the elastic limit there is considerable question regarding what value to use for $\nu$, Poisson's ratio. In the elastic range it is about 0.3 for most metals. Under conditions of complete plasticity it is usually assumed that the volume remains constant, which means that $\nu = 0.5$. Thus for strains which are partly elastic and partly plastic, the factor $1/1\text{-}\nu$ lies somewhere between 1.43 and 2.00. Biaxial fatigue tests are needed in order to resolve this uncertainty. Since most practical designs do not involve strains that go far into the region of complete plasticity, it is recommended that the less conservative value of 0.3 be assumed, and the resulting error be absorbed in the factor of safety.

## Combined Stress[1-4]

When the analysis of stresses in a member reveals a biaxial or triaxial

B. F. Langer is associated with the Westinghouse Electric Corp., Bettis Plant, Pittsburgh, Pa.

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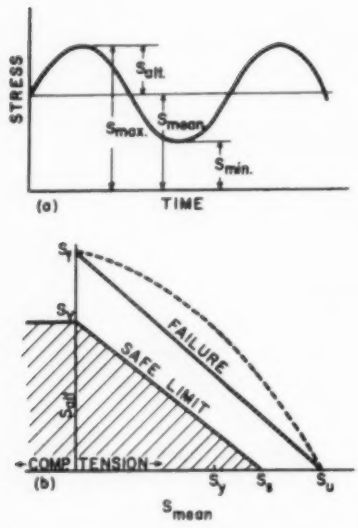


Fig. 1 (*a*) History of fluctuating stress; (*b*) Modified Goodman Diagram for evaluation of fluctuating stress

stress condition, it is necessary to make some assumption regarding the failure criterion which is to be used. The choice is between the maximum shear theory and the Mises criterion (strain energy theory). For biaxial fatigue in the elastic range there is experimental evidence to show that the Mises criterion is the more accurate of the two (see page 7 of Reference 1). In the plastic range, however, where most of the significant thermal stresses lie, there is no experimental evidence available. Therefore, it is recommended that the maximum shear theory be used, since it is a little more conservative and results in simpler mathematical expressions. The steps used in developing the procedure are:

1. Calculate the three principal stresses, $\sigma_1$, $\sigma_2$ and $\sigma_3$.

2. Find the maximum shear stress, which is the largest of the three quantities $^1/_2(\sigma_1-\sigma_2)$, $^1/_2(\sigma_2-\sigma_3)$, $^1/_2(\sigma_1-\sigma_3)$.

3. Multiply the maximum shear stress by 2 to give the "maximum intensity of combined stress." Compare this quantity with the $E\epsilon$ values obtained from uniaxial strain fatigue tests.

Stated more simply, the procedure is: *Use the stress intensity representing the largest algebraic difference between any two of the three principal stresses.*

### Combined Steady and Alternating Stress

Figure 1*a* shows the time history of a fluctuating stress intensity, $S$, calculated by the theory of elasticity and the maximum shear theory, already described (see page 14 of Reference 1 and part 3 of Reference 2). If $S_{max}$ and $S_{min}$ are the maximum and minimum values of

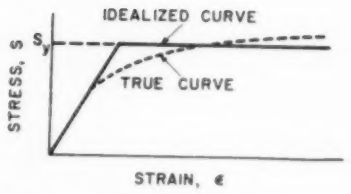


Fig. 2 Idealized stress-strain relationship

the stress intensity which occur during operation, we define two new quantities, $S_{mean}$ and $S_{alt.}$, as follows:

$$S_{mean} = \frac{S_{max} + S_{min}}{2} \qquad (1)$$

$$S_{alt.} = \frac{S_{max} - S_{min}}{2} \qquad (2)$$

Thus we have separated the fluctuating stress into a steady (mean) component and an alternating component. In Fig. 1*b*, if we plot steady components on the horizontal axis and alternating components on the vertical axis, the dotted curve $S_f-S_u$, joining the fatigue limit to the ultimate strength, represents the approximate focus of combinations which result in failure. For simplicity and conservatism this curve may be replaced by a straight line. This construction is known as the "Modified Goodman Diagram." In a rotating-beam fatigue specimen, the stress at any point fluctuates from tension to an equal value of compression, so that $S_{mean}$ is zero, and $S_{alt.}$ is the stress applied to the specimen. This condition appears as a point in the vertical axis in Fig. 1*b*. The diagram in Fig. 1*b* shows the weakening effect of having a mean stress which is different from zero. Allowable stress values for design must be lower than those which produce failure, so the "safe limit" line is drawn below the "failure" line. Any combination of $S_{mean}$ and $S_{alt.}$ which falls inside the shaded area is safe, and any combination which falls above or to the right is unsafe. There is some evidence to show that a compressive mean stress does not reduce the allowable alternating stress. Therefore, for uniaxial stresses, the diagram to the left of the vertical axis may be drawn as a horizontal line, as shown. For multiaxial stresses, the data are incomplete and that which exists has not been reduced to a generally accepted rule; therefore, for the present, it is safer to assume that the diagram is symmetrical about the vertical axis. The allowable values of mean and alternating stresses are called $S_s$ and $S_r$, respectively.

The distance between the "failure" line and the "safe limit" line represents the safety factor being used. The method used for choosing $S_y$ will be described later. When choosing the value for $S_r$, it should be kept in mind

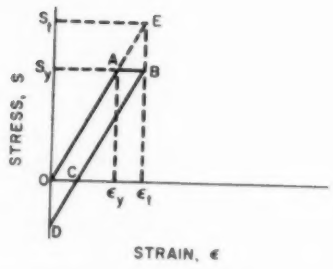


Fig. 3 Stress-strain diagram for $2\epsilon_y > \epsilon_f > \epsilon_y$

that the usefulness of the Modified Goodman Diagram is limited to the evaluation of the imminence of fracture due to cyclic loading, and that it cannot be used to draw any other conclusions. For example, part of the "safe area" is beyond the yield stress in tension, although it is obvious that a mechanical load which produced such a stress would be unacceptable in most cases. Thus the value of $S_s$ merely establishes the slope of the safe-limit line and need not be chosen as conservatively as the value of $S_r$. In Soderberg's modification of the Goodman Diagram, $S_s$ was chosen as equal to the yield stress; but this was done in an attempt to make the diagram a useful criterion for both steady and fluctuating stresses of either thermal or mechanical origin. It is shown in Appendix A that when $S_s = S_y$ certain inconsistencies occur. Therefore, it is recommended that for the present purpose of evaluating the cyclic thermal stress, $S_s$ be taken as the mean between $S_u$ and $S_y$. This value locates the "safe limit" sufficiently below the "failure" line to be conservative and, as will be shown later, provides a transition zone between the elastic and the plastic regions which conforms to the known experimental data.

The foregoing theory has been found useful in the design of components, such as the parts of rotating machines, where the stress fluctuations occur millions of times during the life of the component, and where $S_r$, the allowable amplitude of $S_{alt.}$, is less than the yield strength of the material. It can also be used for cases of thermal stress. In many thermal-stress problems, however, the number of fluctuations during the life of the component is only a few thousand. For ductile metals, the strain required to produce fatigue failure in a few thousand cycles is far in excess of that required to produce yielding. It would, therefore, be uneconomical to limit a strain which will only be cycled a thousand times to a value which would be safe for a million cycles. Thus many cases arise in which $S_r$, the allowable alternating component, is larger than $S_y$, the yield stress. This condition requires further discussion.

In order to visualize what happens when a material is cycled through a strain range larger than its yield strain, let us ignore, for the moment, all secondary effects such as strain hardening and the Bauschinger effect, and assume a material which has perfect plasticity. This is one which has the idealized stress-strain curve, shown by the solid line in Fig. 2, instead of the actual curve shown by the dotted line. Figure 3 shows its stress-strain history as it is cycled from zero to a strain $\epsilon_t$, larger than $\epsilon_y$, the yield strain, but smaller than $2\epsilon_y$. During the first half-cycle it stretches elastically from O to A and plastically from A to B. In the second half-cycle it unloads from B to C and goes into compression from C to D. On subsequent cycles it follows the line DB, back and forth.

The stress analysis of this case, assuming elastic behavior, would have resulted in the values $S_{min} = 0$, $S_{max} = S_t$, $S_{mean} = {}^1/_2\, S_t$, and $S_{alt} = {}^1/_2\, S_t$. Inspection of Fig. 3 shows, however, that the real value of $S_{mean}$ is ${}^1/_2\, S_t - (S_t - S_y)$ which reduces to

$$S_{mean} = S_y - S_{alt} \qquad (3)$$

Stated in words, *the reduction in mean stress equals the amount by which the peak stress exceeds the yield stress.*

Let us now consider a case where the applied strain, $\epsilon_t > 2\,\epsilon_y$. This is shown in Fig. 4. The first half-cycle follows OAB. The second half-cycle goes from B to C elastically, but then reaches the yield stress in compression and goes from C to D with no increase in compressive stress. Subsequent cycles travel around the parallelogram DEBCD. Obviously, the real value of the mean stress is zero; but if we had used the rule given in the preceding paragraph, we would have obtained a negative value. Thus, when $S_{alt} > S_y$, $S_{mean} = 0$.

One more case is needed in order to illustrate the principles involved. This is the case where the cyclic strain range does not exceed $2\,\epsilon_y$, but is superimposed on a static stress, $S_p$. In other words, $S_{min} > 0$. In Fig. 5 the stress follows ABC during the first half-cycle, and CD thereafter. The situation is quite similar to the first case, Fig. 3, and the addition of the static load has not affected either $S_{mean}$ or $S_{alt}$. The lack of influence of steady load on life under plastic strain cycling has been demonstrated experimentally. In Ref. 11 there are reports of tests in which some thermally cycled specimens were clamped at the hot end of the cycle to produce tensile strains and others were clamped at the cold end of the cycle to produce compressive strains. No difference was found in the strain range required to produce failure. In Reference 13 there are reports of tests in which no significant difference was found between specimens cycled from zero to a maximum, and specimens cycled in reversed bending. The life depended on the total strain range. In both cases the strain cycling extended into the plastic range. Large amounts of cold work prior to testing do, however, affect the fatigue properties.[5, 11]

Figure 6 shows how the three cases considered above appear on the Modified Goodman Diagram. The following hypothetical values have been assumed:

For all cases, $S_y = 24{,}000$ psi, $S_u = 60{,}000$ psi, $S_e = 42{,}000$ psi and $S_f = 40{,}000$ psi.

For Fig. 3, $S_{min} = 0$, $S_t = 32{,}000$ psi. Then $S_{max} = 32{,}000$ psi, $S_{alt} = (32{,}000 - 0)/2 = 16{,}000$ psi, $S_{mean} = (32{,}000 + 0)/2 - (32{,}000 - 24{,}000) = 8000$ psi.

For Fig. 4, $S_{min} = 0$, $S_t = 52{,}000$ psi. Then $S_{max} = 52{,}000$ psi, $S_{alt} = (52{,}000 - 0)/2 = 26{,}000$ psi. Since $S_{alt} > S_y$, $S_{mean} = 0$.

For Fig. 5, $S_{min} = 12{,}000$ psi, $S_t = 32{,}000$ psi. Then $S_{max} = 44{,}000$ psi, $S_{alt} = (44{,}000 - 12{,}000)/2 = 16{,}000$ psi, $S_{mean} = (44{,}000 + 12{,}000)/2 - (44{,}000 - 24{,}000) = 8000$ psi.

For the hypothetical numbers that were chosen, all three cases are satisfactory. The interesting point is that the examples in Figs. 3 and 5 appear at the same point on the diagram, even though an additional steady stress was added in Fig. 5. Further study shows that *when $S_t > S_y$, the alternating component is the only factor which determines the safety of the application.* This means that when $S_t > S_y$ there is no need to use the diagram at all, except for the evaluation of cumulative damage, which will be discussed later. The safety of the application may be determined by merely comparing $S_{alt}$ with $S_y$.

It must be noted that the conclusions drawn here refer only to the evaluation of the possibility of fatigue failure by cracking and do not cover the type of failure which might occur as a result of distortion. For example, D. R. Miller* has shown in work not yet published that thermal cycling in the presence of steady mechanical load can cause progressive yielding through a ratcheting process if the magnitudes of the stresses are large enough.

The foregoing discussion has all been based on the assumption of perfect plasticity and an idealized stress-strain curve. Actually many materials have stress-strain relationships nearer to that shown by the dotted line in Fig. 2. A stress-strain diagram such as that illustrated in Fig. 3 would really look more like that shown in Fig. 7, and the cycling following the initial straining OAB would enclose a hysteresis loop BCDB instead of following a straight line. However, the simple procedure proposed here can be shown to be conservative as long as the idealized value of yield stress is chosen sufficiently large. In Fig. 2, as long as the thermal strain studied is less than the strain at which the true curve crosses the idealized curve, the method is conservative be-

* Knolls Atomic Power Laboratory.

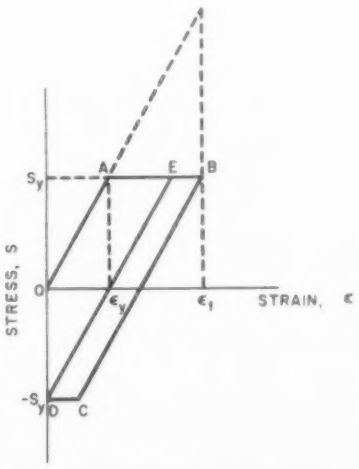


Fig. 4 Stress-strain diagram for $\epsilon_t > 2\epsilon_y$

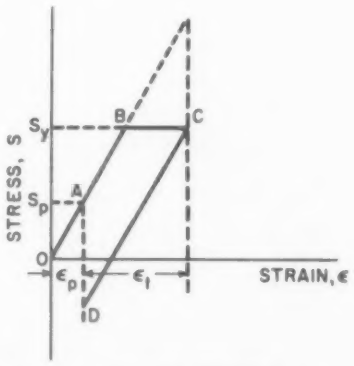


Fig. 5 Stress-strain diagram for $S_{min} > 0$

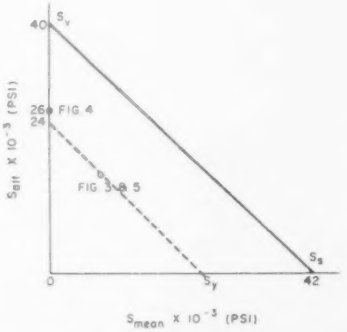


Fig. 6 Modified Goodman Diagram for examples of Figs. 3, 4 and 5

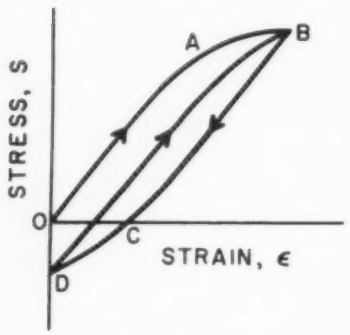


Fig. 7 Realistic stress-strain diagram

cause less credit is being taken for stress reduction due to yielding than actually occurs. The 0.2% yield stress is recommended as a good value to choose for the idealized yield stress because it represents a total strain of 0.3% in a material with an elastic modulus of $30 \times 10^6$ psi and a yield strength of 30,000 psi. This means that for such a material, any conclusions drawn regarding calculated thermal stresses less than 90,000 psi are conservative.

For materials which show considerable strain hardening, such as austenitic steels, the endurance limit for large numbers of cycles may be higher than the yield stress. It is not reasonable to suppose, however, that yielding can occur in every cycle without eventually producing failure. Therefore, it is dangerous to use the yield point of the fully-annealed material in calculating the reduced value of mean stress. A higher yield stress, equal to the endurance limit, should be used instead. What really happens is that strain hardening raises the yield stress up to the endurance limit.

### Stress Concentration

When Goodman's diagram or Soderberg's modification of it are being used for members which contain small regions of stress concentration, the common practice is to ignore the stress-concentration factor when calculating the steady stress component and include it when calculating the variable component (see page 14 of Reference 1). The same principle could be employed in the situations described here, but some caution must be used in its application. If the geometry is such that yielding does not cause a re-distribution of load (for example, a tension member with a reduced section), the full stress values should be used. In the "Summary of Rules" given later on, the conservative procedure of considering stress concentration in both the steady and alternating components is followed.

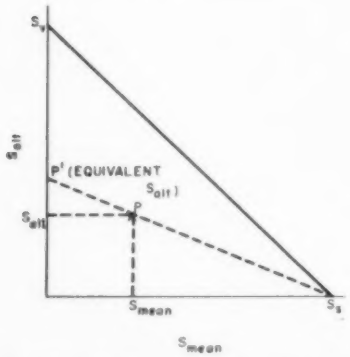


Fig. 8 Determination of equivalent $S_{alt.}$ for cumulative damage

### Cumulative Damage[6-7]

In most practical applications, a component will be subjected to a wide variety of operating conditions, of which some will produce negligible stresses while others will produce stresses worthy of consideration. When we are designing for either static load or large numbers of stress cycles, it is sufficient to design for the condition which produces the highest stress and ignore all others, because we are designing for infinite life and all stresses must be below the value which produces damage. However, as soon as we accept the existence of strains beyond the elastic limit, we are designing for finite life and must consider the damaging effect of all significant strains. If we know the relationship between $S_r$ and allowable cycles, the expected life can be estimated with fair accuracy by adding together the "cycle ratios" produced by each operating condition.

Suppose, for example, we have investigated the stresses produced by $n$ different operating conditions and found that they produce values of $S_{alt.}$ which can be designated as $S_1$, $S_2$, $S_3$ . . . $S_n$. We also know that $S_1$ is repeated $p_1$ times during the life of the component, $S_2$ is repeated $p_2$ times, $S_3$ is repeated $p_3$ times, etc. From the fatigue curve, Fig. 10, we find numbers of cycles $N_1$, $N_2$ . . . $N_n$, which represent the allowable cycles for each of the calculated stresses. Each value of $p$ must, of course, be less than the corresponding value of $N$ because if, for example, $p_1$ were greater than $N_1$ this would mean that $S_1$ was an unacceptable stress. The values $p_1/N_1$, $p_2/N_2$, etc. are called the cycle ratios because they represent the fraction of the total life which each stress value uses up. As a first approximation we might consider that the application is satisfactory if

$$\sum_{i=1}^{i=n} \frac{p_i}{N_i} < 1.0 \qquad (4)$$

Fatigue tests have shown, however, that fatigue failure can occur at cumulative cycle ratios different from unity.[7] If the lower stress values are applied first and followed by progressively higher stress values, the cycle ratio at failure can be "coaxed" as high as 5. On the other hand, if the most damaging stresses are all applied first, failure can occur at cycle ratios as low as 0.6, or even lower. These are extreme conditions. For random distributions, cycle ratios can usually be expected to average close to unity. Therefore, 0.8 is recommended as a conservative allowable limit.

When the strain cycle being investigated involves both a mean and an alternating component, the value chosen for $N$, the limiting number of cycles must reflect the effect of the mean stress. The Modified Goodman Diagram can be used for this purpose. In Fig. 8, if $P$ represents a calculated stress condition, the equivalent value of $S_{alt.}$ to be used in Fig. 10 for finding $N$ can be obtained by drawing a straight line from $S_r$ on the horizontal axis through $P$ to the vertical axis, thus locating point $P'$.

It has previously been mentioned that when the strain range is within the elastic limit, the magnitude of the mean strain has a marked effect on the cycles-to-failure, whereas in the plastic range its effect is negligible. It will be found that the use of the Goodman Diagram as described in the preceding paragraph provides a transition zone between these two regions. For example, in Fig. 9, if we have a mean stress $S_{mean}$ and an alternating stress sufficient to bring the condition to point $P$, the equivalent stress is at point $P'$. If we now increase the cyclic strain range up to and beyond the elastic limit, we arrive at the points $P_1$, $P_2$ and $P_3$, with the corresponding equivalent stresses at points $P'_1$, $P'_2$ and $P'_3$. (Above point $P_1$, the mean stress automatically decreases as the alternating stress increases.) As the alternating stress component increases, the effect of mean stress becomes less and less until finally, when $S_{mean}$ becomes zero, its effect has disappeared.

### Recommended Values of $S_r$

The design methods described here are dependent on the existence of data showing the relationship between strain range and cycles-to-failure for the material being used. The data available are meager, but some have been provided by investigations at three laboratories. Workers at Knolls Atomic Power Laboratory have made uniaxial tests on Type 347 austenitic stainless steel.[8-12] The University of Illinois and Lehigh University have made tests, under the sponsorship of the Pressure Vessel Research Committee, on ferritic steels used in pressure vessels. At the University of Illinois,[16-18] strains were produced by applying pressure to one side of a rectangular plate. At Lehigh, cantilever beam specimens were used.[13-15, 18]

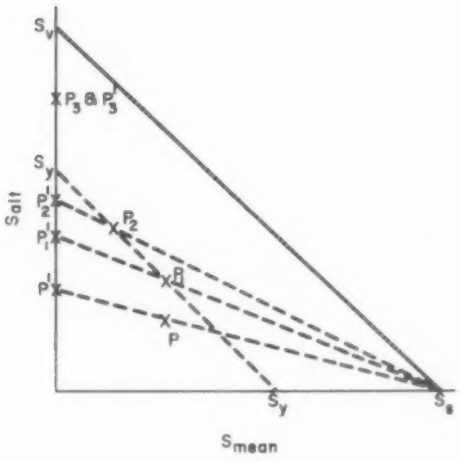


Fig. 9 Transition from elastic to plastic range

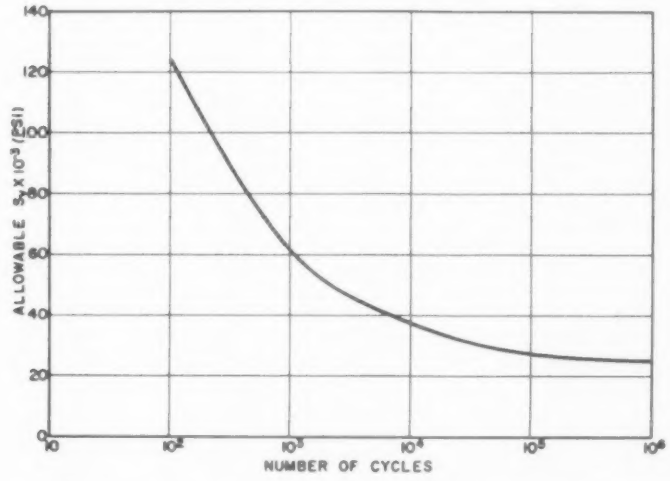


Fig. 10 Allowable values of $S_a$ for Types 304, 347, A201, A212 and A302 steels

The curve of $S_a$ vs. cycles, shown in Fig. 10, is based on the available data and represents the best recommendation that can be made now. It was constructed by drawing a "lower limit" curve through the lowest test points and then constructing a "design curve" which had a factor of safety of at least 10 based on cycles, and a factor of safety of at least 1.5 based on strain range. The original data were reported in the form of total strain range versus cycles. Corresponding values of $S_t$ were obtained by multiplying the total strain range by $1/2\,E$.

In addition to the different steels tested, the data used include such variables as temperature, surface finish and direction of rolling. Tests at temperatures higher than 350° C were not used. Also, tests on severely notched specimens were not used, since it was felt that predictable values of stress concentration should be factored into the stress calculations rather than the material properties.

It is believed that the curve of Fig. 10 can be safely used for the following steels, and for steels with similar properties when in the annealed condition: 347, 304, A201, A212 and A302. Surface finish may be rolled, pickled, machined or any other condition normally considered acceptable for the application. The curve should not be used for high-strength or hardened steels.

## Summary of Rules

In order to evaluate the acceptability of the thermal-stress conditions expected to occur at a given point, proceed as follows:

1. List the various phenomena which might produce thermal stress, and estimate the number of times, $p$, each might occur during the life of the component. The estimate of the number of repetitions should, of course, be conservatively large but nevertheless realistic.

2. From Fig. 10 find $S_a$ for each value of estimated repetitions.

Perform the next three steps for each of the listed phenomena:

3. Consider the value of each of the three principal stresses at the given point vs. time for a complete cycle. The effects of stress concentrations should be included. Call these stresses $\sigma_1$, $\sigma_2$ and $\sigma_3$ for later identification.

4. Consider the stress intensities $S_{12} = \sigma_1 - \sigma_2$, $S_{23} = \sigma_2 - \sigma_4$, $S_{31} = \sigma_3 - \sigma_1$ vs. time for the complete cycle.

5. For each of the three stress intensities, locate the extremes of the range through which it fluctuates and find the absolute magnitude of this range. Call one-half of this range $S_{alt.}$

6. If, for any of the listed phenomena, any one of the values of $S_{alt.}$ ($S_{alt.\ 12}$, $S_{alt.\ 23}$ or $S_{alt.\ 31}$) is greater than the corresponding value of $S_a$ for that phenomenon, the design is unsatisfactory for the given conditions. Either the design must be changed, the method of operation must be changed or the component must be given a smaller value of predicted life.

For each phenomenon follow the next four steps:

7. For each of the three stress intensities locate the mean value of the range through which it fluctuates and call its absolute value $S'_{mean}$. This is the basic value of the mean stress intensity, and the actual value for use in the Modified Goodman Diagram depends on whether or not $S_y$ has been exceeded during the cycle.

(*a*) For any stress intensity for which

$$S_{alt.} + S'_{mean} \leqq S_y$$

the mean stress intensity is equal to its basic value, and

$$S_{mean} = S'_{mean}$$

(*b*) For any stress intensity for which

$$S_{alt.} + S'_{mean} > S_y$$

and

$$S_{alt.} < S_y.$$

$$S_{mean} = S_y - S_{alt.}$$

(*c*) For any stress intensity for which

$$S_{alt.} > S_y.$$

$$S_{mean} = 0.$$

8. Construct the Modified Goodman Diagram with $S_a$ from step 2 and $S_s = 1/2(S_u + S_y)$. Plot the points ($S_{mean\ 12}$, $S_{alt.\ 12}$), ($S_{mean\ 23}$, $S_{alt.\ 23}$), ($S_{mean\ 31}$, $S_{alt.\ 31}$). If any point falls outside the safe limit, the design is un-

satisfactory for the given conditions (see step 6).

9. Find the equivalent value of each $S_{alt.}$ by projecting from $S_y$ on the horizontal axis, through the plotted point, to the vertical axis (Fig. 8).

10. Find the cycle ratio, $p/N$ for each plane, 1–2, 2–3 and 3–1. $p$ is the estimated number of cycles for the phenomenon, and $N$ is the allowable number of cycles from Fig. 10 for the equivalent $S_{alt.}$ found in step 9.

11. For each of the three planes calculate the sum of the cycle ratios, $\sum(p/n)$, for all the listed phenomena. For acceptability, none of the three summations should exceed 0.8.

Experience shows that, for many practical cases, some steps drop out automatically and the necessary procedure is much simpler than that outlined above. For example, it is frequently known that only one type of transient is serious enough to be worthy of consideration. If $S_e > S_y$ for this phenomenon, the application is safe if $S_{alt.} \leqslant S_e$, and unsafe if $S_{alt.} > S_e$. No diagrams need be constructed, and no cumulative cycle ratios need be calculated. For any case in which the yielding causes $S_{mean}$ to drop to zero, there is no difference between $S_{alt.}$ and its equivalent value, and step 9 is unnecessary.

## Example

Assume that the material used in the example of Fig. 6 is used for a pressure vessel and that the internal pressure of 2000 psi produces a constant hoop-stress component at the inside of the cylindrical wall of 10,000 psi. The vessel is to be heated slowly but cooled rapidly. The rapid cooling produces a thermal stress at the inner surface of the wall of 32,000 psi. It is estimated that this heating and cooling will occur 1000 times during the life of the vessel. In addition to the heating and cooling, thermal stresses are also produced by internal heating from nuclear radiation. At full radiation level the thermal stress at the inside of the vessel wall is calculated as 52,000 psi tension, assuming elastic behavior. It is estimated that the radiation level may fluctuate from zero to full value as often as 100,000 times during the life of the vessel.

At a point on the inside of the vessel wall, let

$\sigma_1$ = radial stress component
$\sigma_2$ = hoop stress component
$\sigma_3$ = axial stress component

The following table shows the values of stress components and stress intensities at different stages of the operation. (The vessel is pressurized at all times.)

| | $\sigma_1$ | $\sigma_2$ | $\sigma_3$ | $S_{12}$ | $S_{23}$ | $S_{31}$ |
|---|---|---|---|---|---|---|
| Temperature steady, no radiation | −2,000 | 10,000 | 5,000 | −12,000 | 5,000 | 7,000 |
| Cooldown, no radiation | −2,000 | 42,000 | 37,000 | −44,000 | 5,000 | 39,000 |
| Temperature steady, max radiation | −2,000 | 62,000 | 57,000 | −64,000 | 5,000 | 59,000 |

For the heating and cooling cycle,

$$S_{alt.\ 12} = \frac{44{,}000 - 12{,}000}{2} = 16{,}000 \text{ psi}$$

$$S_{alt.\ 23} = 0$$

$$S_{alt.\ 31} = \frac{39{,}000 - 7{,}000}{2} = 16{,}000 \text{ psi}$$

$$S_{mean\ 12} = S_y - S_{alt.\ 12} = 24{,}000 - 16000 = 8000 \text{ psi}$$

$$S_{mean\ 23} = 5000 \text{ psi}$$

$$S_{mean\ 31} = 24{,}000 - 16{,}000 = 8000 \text{ psi}$$

On the Modified Goodman Diagram of Fig. 6, points ($S_{mean\ 12}$, $S_{alt.\ 12}$) and ($S_{mean\ 31}$, $S_{alt.\ 31}$) coincide at the point (8000, 16,000) previously plotted to illustrate Figs. 3 and 5. The point ($S_{mean\ 23}$, $S_{alt.\ 23}$) represents a point of such low stress that it is obviously of no significance.

For the radiation-change cycle,

$$S_{alt.\ 12} = \frac{64{,}000 - 12{,}000}{2} = 26{,}000 \text{ psi}$$

$$S_{alt.\ 23} = 0$$

$$S_{alt.\ 31} = \frac{59{,}000 - 7{,}000}{2} = 26{,}000 \text{ psi}$$

$S_{mean\ 12}$ = 0 because $S_{alt.\ 12} > S_y$

$$S_{mean\ 23} = 5000 \text{ psi}$$

$$S_{mean\ 31} = 0$$

The only significant point is (0, 26,000), which has already been plotted in Fig. 6 as an illustration of the conditions in Fig. 4. This point applies to planes 1–2 and 3–1.

For the heating and cooling cycle, the projection from $S_y$ in Fig. 6 through the point (8000, 16,000) to the vertical axis gives an equivalent $S_e$ of 19,500 psi. From Fig. 10 we find that $N = \infty$. From the given conditions, $p = 1000$.

For the radiation-change cycle, $S_e$ = 26,000 psi and from Fig. 10 we find $N = 2 \times 10^5$. From the given conditions, $p = 10^5$.

Therefore, for planes 1–2 and 3–1,

$$\sum \frac{p}{n} = \frac{1000}{\infty} + \frac{10^5}{2 \times 10^5} = 0.5$$

For plane 2–3 a similar calculation would obviously give a total cycle ratio of zero. Therefore, the condition studied is satisfactory for the intended service.

*Acknowledgment*

The author is indebted to J. L. Mershon, of the Bureau of Ships, W. E. Cooper, of the Knolls Atomic Power Laboratory, and S. A. Weber, formerly of Bettis Plant, for ideas developed during numerous discussions.

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## APPENDIX A

A reader who is familiar with the literature on interpretation of fatigue data will note that the Modified Goodman Diagram, recommended here for the evaluation of fluctuating stress, differs in some respects from the "Soderberg Diagram," proposed in Reference 3. The difference between the two diagrams is that the Modified Goodman Diagram is based on the ultimate strength, reduced by a safety factor, as the limit of mean stress, whereas the Soderberg Diagram uses the yield stress. The Modified Goodman Diagram was chosen because it was found that the Soderberg Diagram produces an inconsistency when it is used for cases where $S_e$ is slightly less than $S_y$.

Figure 11 shows a case where $S_y$ = 24,000 psi and $S_e$ = 22,000 psi. AB

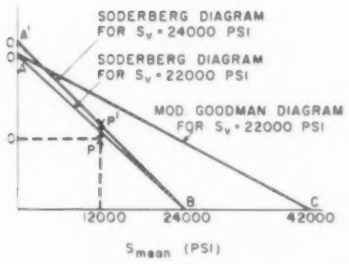


Fig. 11 Comparison between Soderberg and Modified Goodman Diagrams

shows the safe limit according to the Soderberg Diagram and AC shows the safe limit according to the Goodman Diagram. Let us first consider the Soderberg Diagram. Point P shows a condition where $S_{mean}$ = 12,000 psi and $S_{alt.}$ = 10,000 psi, and the condition is safe. If $S_{alt.}$ increases to 12,000 psi, we obtain point P′ which lies above AB, and is consequently unsafe. No appreciable increase in $S_{alt.}$ above 10,000 psi can be allowed. Now note the effect of increasing $S_{alt.}$ the allowable alternating component, from 22,000 psi to 24,000 psi. Not only does P′ become a safe condition, but we find that $S_{alt.}$ can now increase to 24,000 psi, more than double its original value, before the condition becomes unsafe! This is due to the stress reduction from yielding. If $S_{mean}$ starts at 12,000 psi and $S_{alt.}$ increases to 24,000 psi, $S_{max.}$ = 36,000 psi, which is 12,000 above the yield stress. Therefore, $S_{mean}$ is reduced to zero, $S_{alt.}$ = 24,000 psi, and the condition is represented on the diagram by point A′. In the same way, any other value of $S_{alt.}$ between 12,000 psi and 24,000 psi would be represented by a point on the line A′P′. As $S_{alt.}$ increases, $S_{mean}$ decreases by an equal amount.

When the Goodman Diagram is used, the safe limit falls above the diagonal AB and provides a transition zone between the elastic-cycling region $S_r < S_y$, where mean stress is important, and the plastic region $S_r > S_y$, where mean stress is unimportant.

## APPENDIX B—NOMENCLATURE

$\sigma_1, \sigma_2, \sigma_3$ = principal stresses at a point, psi.

$\epsilon_1, \epsilon_2, \epsilon_3$ = principal strains at a point, in./in.

$S$ = equivalent intensity of combined stress based on maximum shear theory, psi.

$T$ = temperature, ° F.

$E$ = modulus of elasticity, psi.

$\alpha$ = linear coefficient of thermal expansion, in./in./° F.

$\nu$ = Poisson's ratio.

$N$ = allowable number of cycles at a given value of $S_{alt.}$.

$p$ = expected number of cycles during the life of a component.

*Subscripts applied to $\sigma$, $\epsilon$, or $S$*

max = maximum algebraic value.

min = minimum algebraic value.

mean = mean algebraic value.

alt. = half-amplitude of full range.

$f$ = fatigue limit of stress.

$u$ = ultimate stress.

$y$ = yield stress or strain.

$s$ = allowable safe limit of $S_{mean}$.

$v$ = allowable safe limit of $S_{alt.}$.

$t$ = stress or strain caused by a change in temperature.

$p$ = stress or strain produced by a static mechanical load, such as pressure.

12, 23, 31 = plane of stress intensity.


**Welding Research Council Bulletin No. 39**

**Plastic Analysis and Design of Square Rigid Frame Knees**

*by John W. Fisher, George C. Driscoll, Jr., and Lynn S. Beedle*

This bulletin contains sample calculations and a description of the plastic design of a square knee for a single-span rigid frame. Welds are designed using the concepts of plastic design as well as those of elastic design in order that the parallelism can be seen. The methods of design presented here can be used as a guide in designing comparable connections for rectangular portal frames. A theoretical analysis of a straight knee with diagonal stiffeners is also presented and leads to expressions for the reinforcement required within the knee to prevent undue deformation. A further analysis is made of the rotation and deflection of the connection.

Single copies of this bulletin may be purchased at $1.00 per copy through the American Welding Society, 33 West 39th Street, New York 18, N. Y.

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# CONTROL OF MELTING RATE AND METAL TRANSFER IN GAS-SHIELDED METAL-ARC WELDING

## PART II—CONTROL OF METAL TRANSFER

*Axial-spray transfer preferred to globular transfer and to rotating-spray transfer to assure maximum arc stability and minimum spatter*

BY A. LESNEWICH

**ABSTRACT.** Metal transfer in gas-shielded welding arcs can be classified as either globular or spray, the spray transfer occurring as either axial-spray or rotating-spray. The globular transfer is characteristic in active shielding gases. It also is developed in inert-gas shields with all electrodes using reverse- or straight-polarity direct current when the current is below some critical level. Above this level, which has been termed the transition current, the metal transfer changes suddenly from globular to axial-spray. This transfer is characterized by the movement, in line with the electrode axis, of minute drops of molten metal from the electrode to the work. It is unique with inert-gas-shielded arc welding and can be achieved with all electrodes when operated with reverse-polarity direct current. With emissive-agent-coated electrodes it can be achieved using straight- or reverse-polarity direct current or alternating current. At some higher level of welding current, the spray of metal suddenly ceases to be transferred in line with the electrode. Its axis begins to rotate rapidly so as to form a conical surface of revolution with its apex at the tip of the electrode. The angle at the apex increases with increasing currents and can cause the development of considerable weld spatter.

The axial-spray transfer is preferred for gas-shielded metal-arc welding to insure maximum arc stability and minimum spatter. This portion of the paper discusses the factors that affect the transition at some current level from globular to axial-spray transfer and, at some higher currents, from axial-spray to a rotating-spray transfer.

The lower transition current is affected by the composition of the electrode and the degree of electrode activation; this is true with both straight-and reverse-polarity direct-current arcs in inert-gas shields. The transition current can be decreased by reducing the electrode diameter or increasing the length of the electrode that extends from the current contact.

The current for transition to rotating-spray transfer also is dependent upon the electrode composition, the electrode extension and the electrode diameter. With straight-polarity direct current, it is affected by the amount and type of emissive-agent coating on the electrode surface.

Curves, equations and photographs are presented to illustrate the various effects described. A discussion also is presented concerning the influence of these effects upon the selection of current, electrode extension, electrode diameter and activation to achieve a particular electrode melting rate and type of transfer.

**A. Lesnewich** is associated with the Research Laboratories of Air Reduction Co., Inc.

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Part II of this paper was presented at AWS Annual Spring Meetings held in Philadelphia, April 11, 1957 and in St. Louis, Mo., April 14–18, 1958.

### Introduction

Inert-gas metal-arc welding has achieved much of its success because of the unique axial-spray transfer of metal that occurs under proper conditions. Such transfer is substantially spatter-free, develops deep weld penetration, and is associated with an arc having self-regulating characteristics.

Spray transfer is characterized by a pointed electrode tip from which minute drops of metal are transferred axially at a very high rate. Around the electrode tip and each drop is a region of incandescent metal vapor; because of the large number of drops in motion and restrictive arc forces, the vapor forms a uniform continuum between electrode and work that is aptly described as the vapor jet. This jet, the electrode tip and the arc crater are, in turn, enveloped by an umbrella of excited atoms of inert gas and metal vapor. A typical axial-spray arc is illustrated in Fig. 16.

Such arcs are found only with inert-gas shields and, when bare electrodes are used, only with reverse-polarity direct current. However, recently developed treatments (activation) of the electrode surface allow the same transfer to be achieved with straight-polarity direct current and alternating current.[5,6] The electrode polarity or activation alone does not control metal transfer; other criteria also must be fulfilled. The purpose of this report is to discuss the factors that affect axial-spray transfer and to demonstrate their influence upon the selection of the proper welding conditions for inert-gas metal-arc welding, especially the limitations upon the usable melting rate.

### Transition Current; Drop to Spray

Low-current reverse-polarity welding arcs in inert-gas shields are characterized by a globular transfer of metal from the electrode. Electromagnetic-pinch effect, gravity and other forces cause these drops to be detached from the electrode and to be accelerated to the work. The globules are frequently larger than the diameter of the electrode; e.g., $^1/_8$-in. diam drops form on $^1/_{16}$-in. diam mild-steel electrodes when the welding currents are in the range of 175 amp. Under these conditions, the drops are seen to be detached at the rate of five drops per sec. The appearance of an arc with globular transfer is shown in Fig. 17.

A gradual increase in current causes an increase in the electrode melting rate and, at the same time, produces an increase in the rate of drop detachment and a reduction in the drop size. At first, the changes in drop size and frequency of transfer are gradual and uniform as shown in Fig. 18 for currents

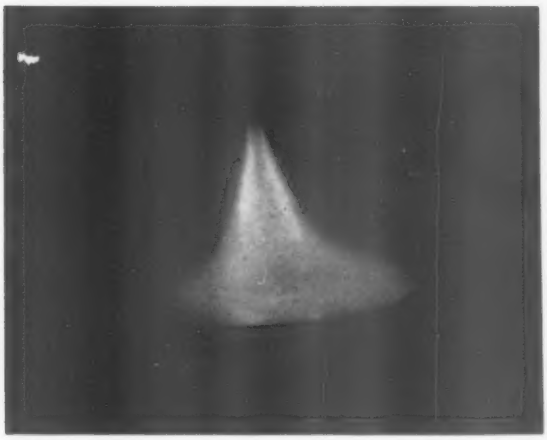

Fig. 16 Typical arc with axial-spray transfer

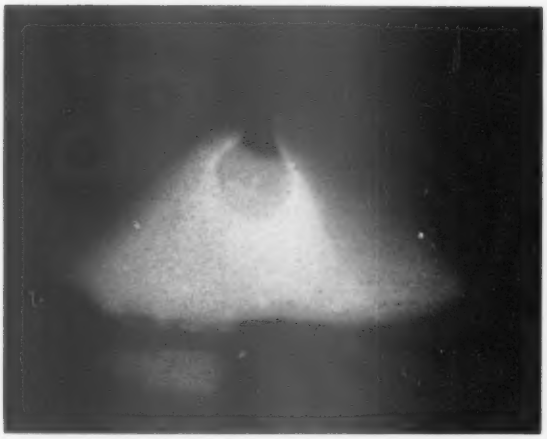

Fig. 17 Typical arc with globular transfer

under 250 amp for the particular set of conditions.

The gradual change of drop size and frequency of transfer does not continue indefinitely. When the welding current is increased beyond some critical level, the drop size is reduced very suddenly and the frequency of their release from the electrode increases markedly. At the same time, a sharp tip is formed on the electrode. The curves in Fig. 18 illustrate the suddenness of these changes. Under the conditions described, an increase in current from 255 to 265 amp caused the drop frequency to be changed suddenly from 15 drops per sec to 240 drops per sec, while the drop diameter was reduced from 0.16 to 0.04 in. The median current at which these discontinuities occurred is called the transition current; in this case it was 260 amp.

Although the transition current can be obtained with data derived from oscillographic or photographic measurements of the type used to plot Fig. 18, this value can be obtained more conveniently with visual observation because the marked change in transfer occurs within a current range of 10 amp. Visual observance of a magnified image of the arc projected on a screen was used to obtain most of the data presented in this paper. The transition currents were found by increasing the current and wire feed simultaneously while holding the arc length fixed until a change from drop to spray transfer was seen. Although the points in Fig. 18 were obtained from ultra-slow motion pictures and oscillograph traces, visual observations of the arc yielded the same numerical value of the transition current.

The sharp change in the mode of metal transfer achieved by increasing the current through the transition point did not appear to be accompanied by a sudden discontinuity of the electrode melting rate. It changed, of course, but only by the amount expected as the result of the same increase of current without a change in the mode of transfer. The frequency of drop transfer continues to increase with current above transition. Figure 18, as an example, illustrates an increase from 240 to 280 drops per sec with a current increase from 275 to 500 amp using a mild-steel electrode and a shielding gas of 99% argon–1% oxygen. The same transition phenomenon occurs with all metals, albeit, at different levels of current.

Most applications of inert-gas metal-arc welding make use of axial-spray transfer. Such arcs are considerably more stable than those below the transition current and they produce substantially no weld spatter because the short circuits associated with globular metal transfer do not occur. With spray, the metal is transferred in line with the electrode even when the electrode is at a slight angle with respect to the work as shown in Fig. 19. This arc stiffness is achieved as a result of interacting magnetic fields and the small size of the drops, and is highly advantageous since the drops can be easily directed into horizontal fillet welds or into overhead groove welds without affecting the arc behavior. In addition, the concentration of energy in the central region of the arc and the kinetic energy of the metal drops striking the weld pool produce deep weld penetration.

The latter feature is not always desirable for out-of-position welding of mild steel since the very fluid, superheated weld metal produced by axial-spray transfer is difficult to control. With axial-spray transfer, vertical-down welds can be made without weaving, but it is almost impossible to use weave techniques for depositing vertical welds in heavy sections since the weld pool is too fluid to be self supporting. Such out-of-position welding is possible, how-

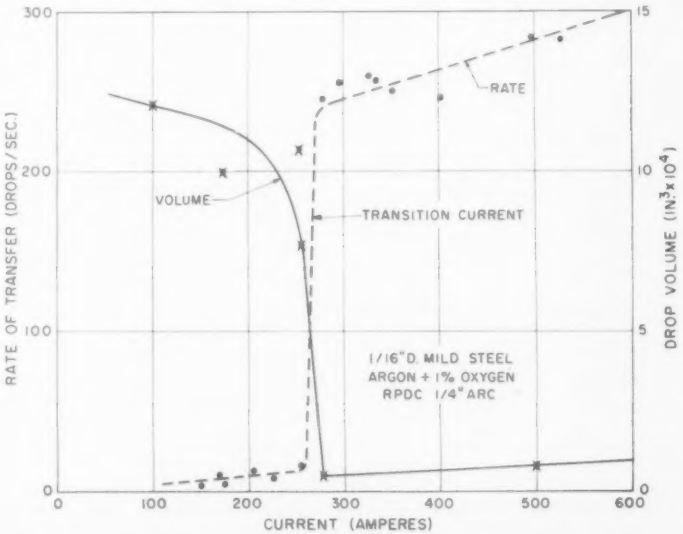


Fig. 18 Effect of current on the size and frequency of drops transferred in an argon-shielded arc

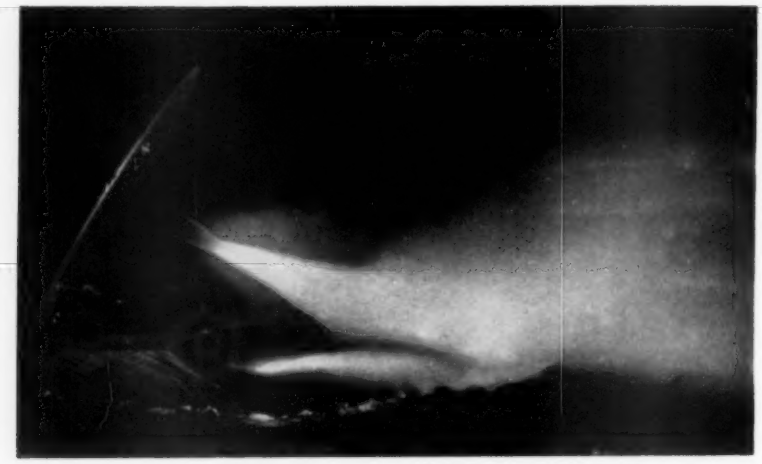

Fig. 19 Example of arc stiffness associated with axial-spray transfer

ever, with the drop transfer that is developed below the transition current because the larger drops are not excessively superheated and the forces within the arc stream are substantially reduced. As a result, the drops are not blown away from the weld pool by the arc stream and can freeze quickly on contact with the work.

Aluminum alloys are easily welded out-of-position with axial-spray arcs because the transition occurs at such relatively low currents that the arc forces are minimized. In addition, the higher thermal conductivity of these alloys causes very rapid freezing of the weld metal.

A number of circumstances arises that makes it desirable to change the transition current. For example, in out-of-position welding, an increase in the transition current is sometimes desirable to allow a higher deposition rate with globular transfer. On the other hand a reduction in the transition current would make possible a stable, spray transfer at the low currents necessary for satisfactory welding of sheet metal. These two examples of the need for a means to control the transition current are the most common, although others of equal importance have been noted.

The transition current can be modified somewhat since it is affected by a number of variables, the most important of which are the electrode composition and diameter, the extension of the electrode beyond the current contact, the type and degree of electrode activation and the electrode polarity. All of the factors will be discussed in the following paragraphs. Most of the information is based on data obtained with a commercial mild-steel electrode shielded with a mixture of 99% argon and 1% oxygen. Nevertheless, the same general results have been shown to be applicable to all other commercial electrodes, and with pure inert-gas shields as well as the inert-gas shields containing oxygen.

## Factors Affecting Minimum Current for Spray Transfer

### *Electrode Composition*

The current at which the transition from drop to spray transfer occurs is dependent in part upon the metal being welded. Aluminum and steel, for example, do not have the same transition current; argon-shielded aluminum arcs develop spray transfer at a current that is approximately one-third lower than that needed with mild steel. Even the minor variations in analysis which are normally encountered among mild-steel electrodes can produce large changes in the transition current. As an example, some electrodes with a diameter of $^1/_{16}$ in. and a fixed extension from the current contact have been shown to achieve spray transfer with currents as low as 220 amp while others in the same general family did not develop spray transfer until the welding current was raised to 310 amp. A good explanation cannot be offered for the effect of chemistry on transition current. There is some reason to suspect that a critical concentration of metal vapor in the arc stream is necessary to initiate the unique spray transfer. Consequently, the presence of alloying elements that boil at low temperatures might be influential in lowering the transition current.

The dependence of transfer on wire composition may present difficulties if welding conditions are inadvertently specified within the range of the transition current. Under such conditions, the minor changes in electrode chemistry that occur from heat to heat can cause the metal transfer to be globular or spray at random. Even the changes in chemistry along a wire due to the normal heterogeneity of an ingot have been found to cause changes in the characteristics of metal transfer when working close to the transition current.

### *Electrode Diameter and Extension*

The electrode geometry, its diameter and amount of extension from the current contact, also have a strong influence on transition current. For any given electrode extension, the level of the transition current increases in almost direct proportion to the electrode diameter as shown in Fig. 20. The transition current decreases with an increase of the electrode extension as shown in Fig. 21.

The data shown in Figs. 20 and 21 are not surprising if the hypothesis is correct that spray transfer is initiated after some critical amount of metal vapor is present in the arc stream. The electrical resistance heating of the electrode already has been shown in Part I of this report to increase by using smaller diameters or longer extensions. As a result, additional heat is available to cause a greater amount of metal vapor to be released from the electrode tip to the arc stream.

However, the changes in resistance heating alone are not responsible for the changes in transition current; neither is the transition current linearly dependent upon current density. If such were the case, the minimum current for spray would be proportional to the cross-sectional area of the electrode and inversely proportional to its extension. Instead, within some limits of error, this current is proportional to the diameter and extension, and can be defined by this empirical relationship:

$$I_s = a + bD - cL \qquad (14)$$

where $I_s$ is the transition current in amperes; $D$ is the electrode diameter, $L$ is the electrode extension; and $a$, $b$ and $c$ are empirical coefficients whose values are seemingly dependent upon both the composition and surface treatment of the electrode. The minimum current for spray is not linearly dependent upon the electrode cross-sectional area but its diameter, and the dependence on extension is not reciprocal. In the case of a typical commercial mild-steel electrode, the empirical equation relating the minimum reverse-polarity direct current for spray transfer to the electrode dimensions is as follows:

$$I_s = 60 + 3400D - 30L \qquad (15)$$

where $D$ and $L$ are measured in inches, and $I_s$ in amperes.

### *Electrode Activation and Polarity*

A globular transfer of metal is normally associated with bare electrodes and straight-polarity direct-current arcs. Even at extremely high currents, axial-spray transfer of metal has not been found. Here, the term "bare" means that the electrode contains no activating materials.

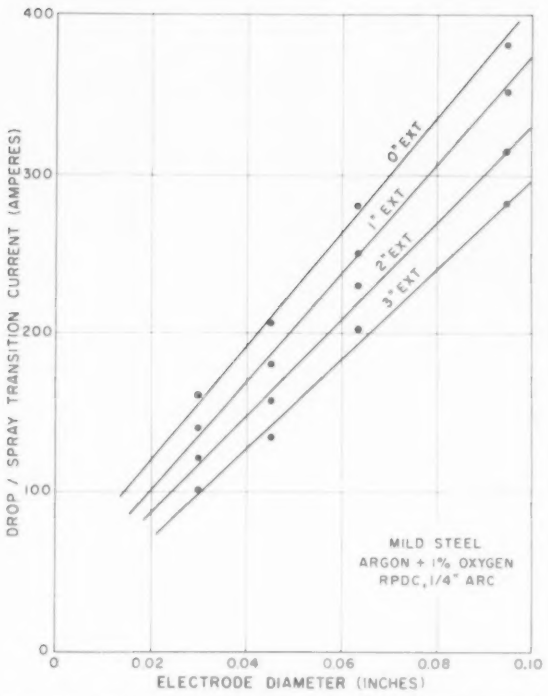


Fig. 20 Increase in transition current from drop to axial-spray transfer caused by increasing the electrode diameter

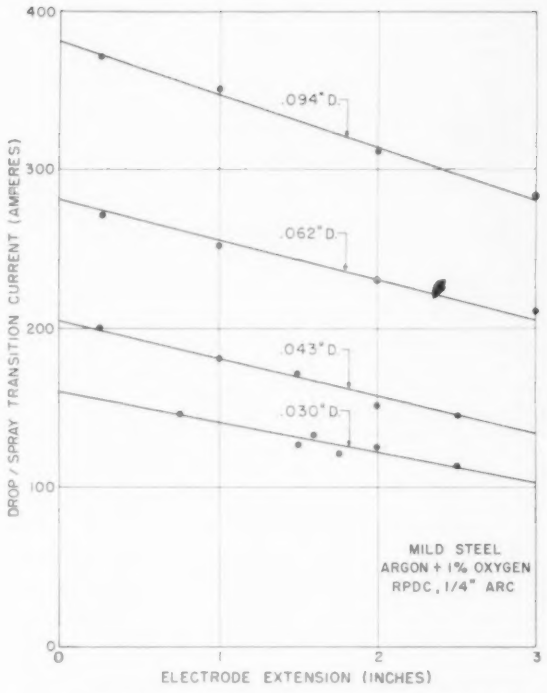


Fig. 21 Reduction in transition current from drop to axial-spray transfer produced by increasing the electrode extension

The addition of a sufficient quantity of metals of the alkali, alkaline-earth and rare-earth families to the surface of a bare electrode allows a spray transfer to be achieved in an inert-gas shield at straight polarity and moderate current levels. The magnitude of current at which such transfer is developed is dependent upon the amount of the activating element that is present and its efficiency. With a fixed electrode diameter and extension, the transition current decreases as the concentration of the activating element is increased. As an example, the effect of cesium on the transition current of a steel electrode coated with cesium phosphate is illustrated in Fig. 22. The transition current is shown to be reduced from 600 to 200 amp as the result of an increase in the cesium concentration from 0.002 to 0.012%. This additive causes only a slight reduction in the transition current of an electrode with reverse-polarity direct current. These data for straight polarity are based on a given, fixed electrode extension. The efficiency of an emissive coating is somewhat dependent upon the preheat in an electrode. The transition current for straight-polarity direct-current arcs can be reduced by the use of emissive coatings to a value below that possible with like coatings and reverse-polarity direct-current arcs. Figure 22 demonstrates the straight-polarity transition current being lowered below that of reverse polarity when over 0.008% cesium is present.

The activation technique is particularly useful for reducing the transition current when it is necessary to effect a substantial reduction in the deposition rate of a particular diameter of electrode without having to tolerate globular transfer. The melting rate of the electrode is lowered somewhat more than would be expected by the change in current since the addition of activation compounds causes a simultaneous reduction of the thermionic work function of the electrode material. The lower work function has been shown[6] to lower the melting rate even at a fixed value of current.

### *Control of Melting Rate*

One of the principal reasons for changing the wire diameter or extension is to control the electrode melting rate at a particular current. When welding thin sections, low currents are desirable to deposit a minimum amount of weld metal and to prevent melting through the joint. In addition, spray transfer is highly desirable for maximum stability of the arc and uniformity of the weld deposit. Spray transfer can be achieved at lower currents by reducing the wire diameter or increasing the electrode extension as already shown in Fig. 21. However, the same changes in electrode extension and diameter may cause the melting rate to increase even though the

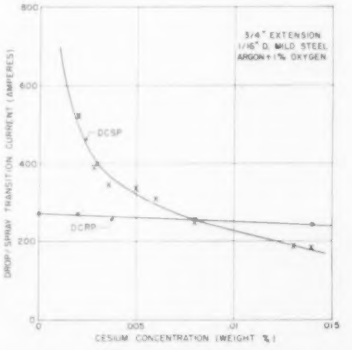


Fig. 22 Reduction in transition current produced by cesium additions to the electrode surface

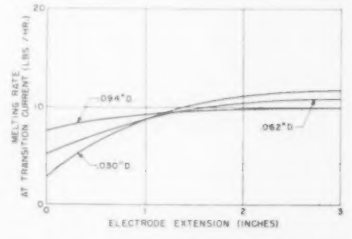


Fig. 23 Melting rate at the transition current from drop to spray transfer with various diameters and extensions of mild-steel electrode. (Mild-steel electrode, argon + 1% oxygen shield, RPDC)

arc is being used with lower currents. The electrical resistance produced by welding current flowing through the electrode causes melting of the electrode at a rate, $M_R$, which for mild steels is defined by the following general equation:

$$M_R = 3.7 \times 10^{-8} \frac{LI^2}{A^{1.88}} \quad (16)$$

where $L$ is the electrode extension in inches, $A$ its cross-sectional area in square inches, and $I$ the magnitude of current in amperes. The constant of proportionality is related to the electrode's electrical resistivity. An increase in $L$ or a reduction in $A$ will cause the melting rate to increase at a fixed level of current. Obviously, the current must be reduced to keep the melting rate fixed if the electrode extension is increased and/or its diameter is reduced. In the same way, a reduction of the transition current by increasing the electrode extension may cause the melting rate at the transition current to be higher, in spite of the reduced current level; i.e., the reduction in the value of $I^2$ in the above equation may be less than the increase in $L$.

In practice, the melting rate at the transition current can be lowered substantially by decreasing the electrode diameter when the electrode extension is kept at a minimum. This is demonstrated by the solid curves of Fig. 23. When the electrode extension is at a minimum, a reduction of the electrode diameter from 0.094 to 0.030 in. causes the melting rate at the transition current to drop, but, for a long extension, the increase in resistance heating produced by a reduction in electrode diameter will cause the melting rate to increase. When the extension is greater than 1 in., the melting rate of the small-diameter electrode at the transition current is greater than that of the large electrode even though the transition current for the small electrode is lower. This is demonstrated by the data of Table 5.

Consequently, when it is necessary to achieve spray transfer with low currents and low deposition rates, a small-diameter electrode and short extension are mandatory. However, most applications of inert-gas metal-arc welding use currents that are in excess of the transition. For these, it is possible to select numerous combinations of electrode diameter and extension that will supply a required melting rate at some fixed value of current. It is generally more economical to select the largest possible electrode diameter that can be used with some practicable amount of electrode extension.

**Table 5—Changes in Melting Rate Caused by Changes in the Electrode Diameter, Extension and Transition Current**

| | *Transition current, amp* | | *Melting rate at transition current, lb/hr* | |
|---|---|---|---|---|
| *Electrode diam, in.* | *1/2-in. extension* | *2-in. extension* | *1/2-in. extension* | *2-in. extension* |
| 0.030 | 160 | 120 | 4.8 | 11.2 |
| 0.062 | 275 | 230 | 6.3 | 10.3 |
| 0.094 | 370 | 315 | 8.3 | 9.7 |

## Transition Current; Axial Spray to Rotating Spray

The previous sections of this paper have described how a sudden change from globular to axial-spray transfer of metal occurs when the magnitude of current is raised beyond some critical level. This value of current has been termed the transition current. If the magnitude of current and electrode feed rate are continually increased while holding the arc length and electrode extension fixed, a second level of current will be reached beyond which the arc characteristics again change. Above this level, the tip of the electrode is caused to bend and rotate about the major axis of the body of the electrode, and the metal projected from the electrode streams off the end of the deflected tip in the direction the tip is pointing. Continued increase of current will increase the angle of bend at the electrode tip until it is almost at right angles to the axis of the electrode. At this time, the metal is no longer transferred axially to the weld pool but is spewed in all directions as spatter, and the arc is no longer useful in the usual or conventional manner as a tool for welding. This type of transfer has been termed rotating spray. It develops at some critical value of current and, as with the transition from drop to axial-spray transfer, the current at which it occurs is dependent upon the polarity, electrode diameter, extension, composition and electrode activation.

A somewhat advanced form of this rotating-spray transfer is illustrated in Fig. 24. By visual inspection, the arc appears to be split into two distinct

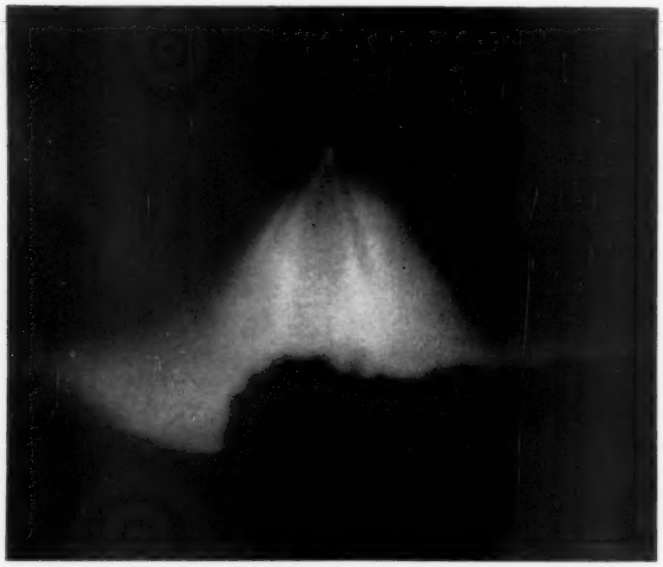

Fig. 24 Typical arc with rotating-jet transfer

Fig. 25 Typical weld bead and spatter produced with a rotating-jet arc

spray arcs at an angle with the axis of the body portion of the electrode. Actually, the arc is rotating to form a cone whose apex is at the axis of the body portion of the electrode. Ultra-slow motion pictures of these arcs show that the electrode tip and vapor jet are rotating and the metal is released at an angle from the electrode.

At currents just above that where this rotation is initiated, the rotation is not always undesirable since it causes the weld bead to widen somewhat and prevents the formation of the very deep papillary penetration that is characteristic of inert-gas metal-arc welding. However, at currents far in excess of the transition from axial spray to rotating spray, it cannot be tolerated because of the spatter. Figure 25 illustrates the type of spatter produced with the arc illustrated in Fig. 24. At higher currents, a swirling arc and considerably more spatter would be developed.

An increase in welding current causes the electrode tip extension to become progressively more plastic due to its own resistance heating. Simultaneously, the increase in current causes the reaction forces due to the release of metal vapor and drops to become stronger. Beyond some critical level of current, the forces become great enough to cause the plastic electrode tip to be deflected and rotated in much the same manner as the free

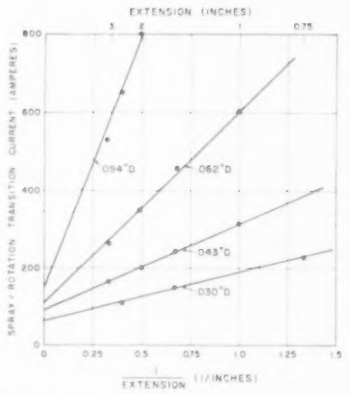


Fig. 26 Increase in current for the transition from axial-spray to rotating-jet transfer produced by decreasing the electrode extension

end of a suspended garden hose that is supplying water. As the current is increased further, the electrode becomes still more plastic while, at the same time, the forces in its tip are stronger so that the degree of deflection is greater and a spattery arc is developed.

The exact current at which the rotating spray begins is somewhat more difficult to find than that at which the change from drop to axial spray occurs. However, with the aid of a projected and magnified image of the arc, the transition current was defined as that level at which twin arcs seemed to develop.

The postulate that electrode plasticity is responsible for rotation gains validity from the fact that the same geometric factors that cause the electrode tip to be heated by resistance to a highly plastic or even liquid state also are those that control the level of current at which the rotating phenomenon is initiated. In addition, those variables that control resistivity of the electrode have a proportional effect upon the current at which the rotating spray is formed.

## Factors Affecting Current at Which Rotation Begins

### *Electrode Extension*

One of the variables affecting the current level at which rotation begins is the electrode extension. This current level is directly related to the reciprocal of electrode extension as shown in Fig. 26. Less current is needed to initiate rotation with increasing extensions if the diameter and composition of the electrode are not changed. As an example, an 0.062-in. diam mild-steel electrode will develop rotation with currents in excess of 600 amp for an extension greater than 1 in. and with currents

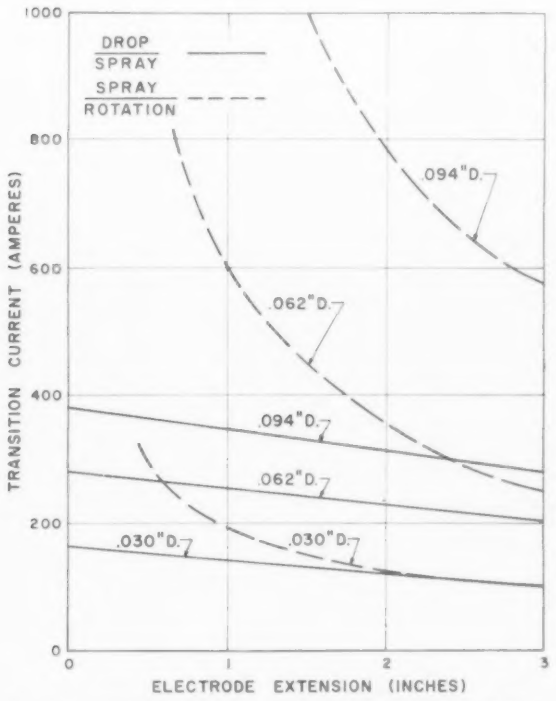


Fig. 27 Changes in the transition current from drop to axial-spray and axial-spray to rotating-spray arcs caused by changes in the electrode extension and diameter. (RPDC, mild-steel electrode, argon + 1% oxygen shield)

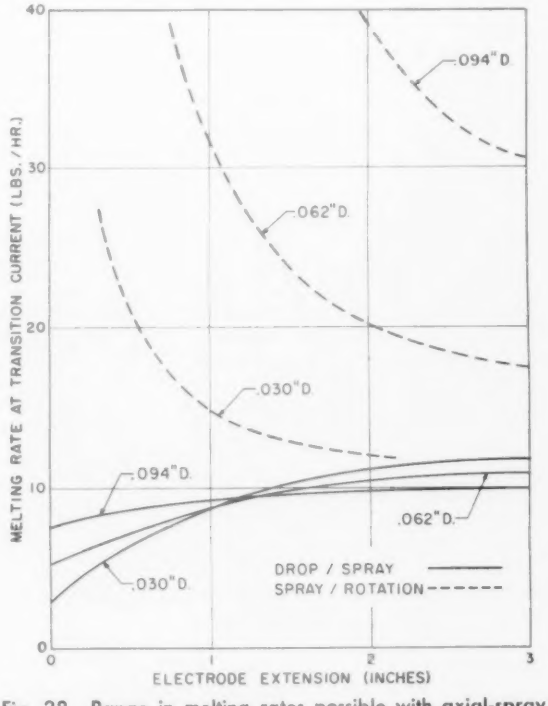


Fig. 28 Range in melting rates possible with axial-spray transfer using various electrode extensions and diameters. (RPDC, mild-steel electrode, argon + 1% oxygen shield)

over 260 amp for a 3-in. extension. This effect of extension on the transition current is not surprising since less current is needed with long extensions to produce the amount of resistance heating that is required to reduce the stiffness of the electrode to the point where the arc forces can initiate rotation.

#### Electrode Diameter

Large-diameter electrodes are mechanically stiff and have relatively low resistance to the flow of current. Therefore, higher currents are needed to achieve rotation when the electrode diameter is increased while holding its extension fixed. This effect of the electrode diameter is illustrated in Fig. 26. As an example, with a fixed extension of 2 in., 800 amp are needed to achieve rotation with the 0.094-in. diam electrode but only 125 amp are needed with an electrode diameter of 0.030 in. With small-diameter electrodes, advantage might be taken of the wide, flat, low-penetration deposit that can be produced with controlled rotation for use in cladding and similar operations. With long extensions, such overlays can be made at reasonably low currents so that dilution will be minimized. Rotating arcs also are useful for deep-groove welding since the weld metal is better directed at the joint walls.

The effects of extension and diameter are interrelated as shown in the following empirical equation for a mild-steel electrode with a reverse-polarity direct-current arc in an argon shield:

$$I_R = 25 + 1350D + 14.5 \times 10^4 \frac{D^2}{L} \quad (17)$$

where $I_R$ is the lowest current at which a rotating spray is produced with an electrode having a diameter of $D$ inches and an extension of $L$ inches. The middle term of this equation may demonstrate the effect of diameter on the electrode stiffness. The last term of this equation is proportional to the reciprocal of the electrical resistance ($\rho L/A$) of the electrode, and therefore, is equivalent to the conductivity of the electrode. Rotation of the electrode tip cannot develop until a reasonable length is heated by its own resistance to a highly plastic or liquid state. An increase in conductance caused by changing the electrode dimensions makes it necessary to increase the magnitude of welding current to achieve the heating necessary for rotation of the electrode tip.

#### Electrode Composition

The previous section has described the effect of the electrode conductance upon the magnitude of welding current required to develop rotation of the electrode tip. The conductance is directly proportional to the conductivity of the electrode and, for this reason, the transition current from axial-spray to rotating-spray is dependent upon the electrode composition. Aluminum electrodes have a high conductivity; approximately five times that of steel. As a result, when a $^1/_{16}$-in. diam aluminum electrode having a 2-in. extension was evaluated, a rotating-spray transfer was not observed at 800 amp although a steel electrode of the same dimensions developed rotating spray at 350 amp. The rotation does develop with stainless steel, other ferrous alloys and bronzes.

#### Electrode Activation and Polarity

Rotating-spray arcs have not been found with bare electrodes and straight-polarity direct current. This is probably due to the relatively large cross-sectional area of the blunt tip, the absence of a vapor jet in the arc, and the presence of relatively massive metal drops. Activation of the electrode produces a pointed tip, a vapor jet and a reduction in drop size that are typical of reverse-polarity arcs. These changes allow the development of rotating-spray transfer. The current at which it develops is controlled by the degree of activation since this modifies the shape of the electrode tip. Although rotating-spray transfer has not been found when the massive drops typical of untreated electrodes with straight-polarity arcs are present, it can be made to occur at reverse polarity when small-diameter electrodes and long extensions are used.

Changes in electrode extension have a much greater effect upon the level of current for transition from axial to rotating spray than upon the current for transition from globular to axial-spray transfer as shown in Fig. 27.

Because of this, unusually long extensions allow the level of current at which rotation of the arc begins to approach that at which spray is initiated. The extension can be made so great that rotation of the electrode tip can be produced with globular transfer. This condition is more probable with small-diameter electrodes. Figure 27 shows that this occurs with 0.030-in. diam steel electrodes when the extension is greater than 2.25 in.

### Discussion

The melting rate of an electrode is controlled by a number of factors. They include the current magnitude and polarity, the electrode diameter, extension, and resistivity and the degree of electrode activation. For reverse-polarity direct-current arcs, melting rate in pounds per hour can be defined by the equation:

$$M_{RP} = (a + bD)I + c\rho \frac{L}{D^{2.5}} I^2 \quad (12)$$

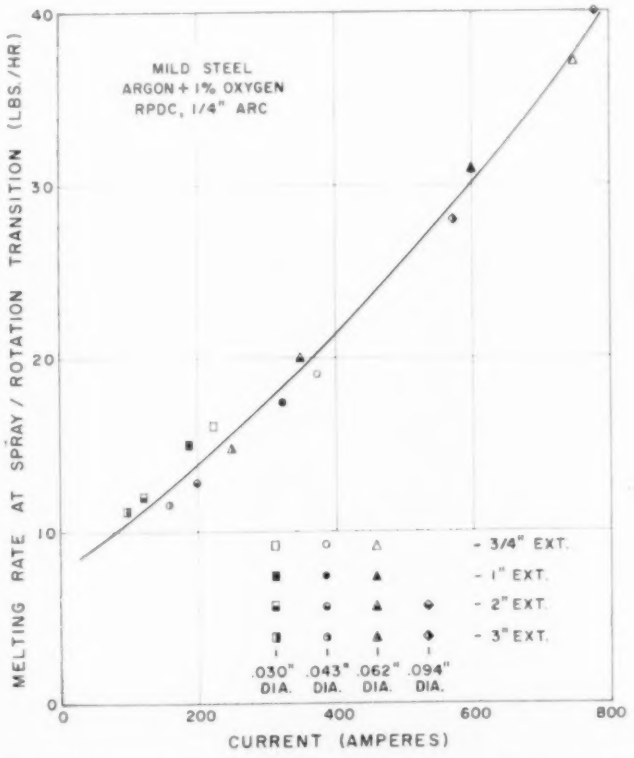


Fig. 29 Dependence on current of the maximum melting rate with arcs having axial-spray transfer

where $a$, $b$ and $c$ are constants of a particular electrode composition, $I$ is the welding current, $\rho$ is the electrode resistivity, $L$ is the electrode extension and $D$ is the electrode diameter.

The same variables also affect the minimum current with inert-gas arcs at which axial-spray transfer will develop and the current at which rotating spray begins. The magnitude of the current necessary to cause these changes is defined by the following general equations:

$$I_S = a' + b'D - cL$$

$$I_R = a'' + b''D + c''\frac{D^2}{L}$$

For most welding applications, it is desirable to utilize the axial-spray arc. An increase in extension and reduction in the diameter of the electrode will lower the minimum value of current, $I_S$, for spray transfer. Simultaneously, the maximum level of current at which axial spray may be maintained, $I_R$, also is lowered but at a much higher rate.

Figure 27 shows that long electrode extensions restrict the range of welding currents producing axial-spray transfer. This is most apparent with small-diameter electrodes. The range of current for an 0.030-in. diam electrode is between 150 amp where drop transfer occurs, and 300 amp where rotating spray occurs when its extension is $^1/_2$ in. However, using an 0.062-in. diam electrode and $^1/_2$-in. stick-out, axial-spray transfer is obtained between 260 and 1000 amp, and can be obtained with extensions in excess of 3 in.

The limitations that are imposed upon the maxima and minima of current also restrict the deposition rates that can be achieved. An example of practicable limitations of melting rate for various extensions and diameters of a mild-steel electrode is illustrated in Fig. 28. The lowest melting rates using axial-spray arcs are achieved with small-diameter electrodes and short extensions. The highest possible practicable deposition rate with axial-spray arcs is achieved with the largest possible diameter of electrode and the shortest possible extension. These increases in melting rate with short extensions are, of course, achieved by increasing the current.

When high deposition rates must be obtained with low welding current, it is possible frequently to select a small electrode diameter or increase the electrode extension or both and still work with axial-spray arcs. But the maximum possible melting rate with axial-spray transfer is normally dependent upon the welding current alone and, thus, no combination of diameter and extension of the electrode will allow the rate established by the current to be exceeded. This is due to the fact that much higher currents are needed to establish rotation of the electrode tip with short electrode extensions or large electrode diameters. The change in current, however, has a much greater effect on melting rate of the electrode than does a change in its extension. Consequently, an increase in extension causes a reduction in the maximum melting rate without rotation. Figure 29 demonstrates the dependence on current of the maximum deposition rates with axial-spray transfer and that no combination of electrode extension or diameter will allow this level of melting rate to be exceeded without the production of spatter. The highest rates of melting are achieved by using short electrode extensions, large electrode diameters and high currents. As an example, with a 350-amp reverse-polarity direct-current arc, a maximum of 19 lb/hr of mild-steel weld metal can be deposited with a 0.043-in. diam electrode having an extension of $^3/_4$ in. or with an 0.062-in. diam electrode of the same composition having an extension of 2 in. In order to increase the deposition rate in excess of 19 lb/hr, it is necessary to increase the current and use a different combination of electrode dimensions; roughly 38 lb per hr can be deposited at 800 amp with .062-in. diam wire having a $^3/_4$-in. extension or 0.094-in. diam wire with a 2-in. extension. This dependence of the maximum electrode melting rate upon current is shown in Fig. 28 if the maximum rate is defined by the initiation of rotation.

If rotating-spray transfer were desired from the electrode used in this study for surfacing, reducing weld penetration, welding in a deep groove and other applications, Figure 28 also could be used to determine the minimum current that is needed for this wire analysis at a particular melting rate. The electrode extension for a particular diameter is found from Figs. 23 and 27.

## Conclusions

1. Three types of transfer can be obtained with the inert-gas metal-arc process. They are globular, axial spray and rotating spray.

2. The type of transfer obtained is determined by the welding-current magnitude and polarity, the wire composition and activation. It also is affected by the wire diameter and the wire extension (the amount of wire extending from the current contact).

3. Axial-spray transfer can be obtained at a lower current by using a small-diameter electrode and a long extension. The melting rate at transition current with spray transfer can be reduced by using small-diameter electrodes and short extensions.

4. Rotating-spray transfer can be used to achieve wider weld deposits and to ensure wetting of the side walls in deep-groove welding.

5. The upper end of the current range for conventional inert-gas metal-arc welding is that at which excessive spatter forms due to the rotating spray.

6. Spatter-free arcs with high deposition rates in the conventional process require high currents; no combination of wire diameter and extension will allow the maximum melting rate for a particular critical level of current to be exceeded. Short extensions and large diameters of electrodes are desirable for such applications.

*Acknowledgment*

The author wishes to express his appreciation to the Air Reduction Co., Inc., for its permission to present this paper and to his colleagues for their helpful assistance. He wishes to express special thanks to C. W. Hoffman for his able performance of most of the experimental work.

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# COOPERATIVE INVESTIGATION OF A NEW WELDING ELECTRODE FOR STAINLESS STEEL

*Several different laboratories participate in evaluation of a new welding procedure for use in high-temperature steam piping*

BY R. D. WYLIE

ABSTRACT. The data presented in this paper are the result of tests in a number of different laboratories and represent a summary of the work of many groups in evaluating a new welding procedure for use in high-temperature steam piping. The author feels that much has been accomplished by these welding procedures in minimizing fabrication problems, but that more work should be done to produce wrought materials with better weldability characteristics.

## Introduction

In the past several years, a number of technical papers has been written discussing welding difficulties experienced with Type 347 stainless steel. This steel was extensively used for components of nuclear-power plants and for piping for the nation's most modern steam-generating stations. It was natural, therefore, that the difficulties experienced in fabrication and service received wide discussion, and a considerable amount of effort was expended to find suitable solutions to the problem. A good description of the Type 347 cracking problem was given by Poole,[1] who presented the results of an AEC-sponsored survey. This survey revealed that all fabricators of Type 347 stainless steel experienced some type of cracking during the fabrication of Type 347 vessels. Because of delays experienced in critical components for the atomic-energy program this was a very serious problem.

Industry followed two approaches which might lead to a solution of this problem. The first approach was to determine the basic cause of the cracking and thus, if possible, to improve these steels based on these findings. Investigators such as Nippes and Savage,[2,3] Pellini and Puzak,[4] Heuschkel,[5] Curran and Rankin,[6] Soldan and Mayne,[7] and the author[8] have proposed various theories to explain the cause of this cracking, but despite these theories no fundamental improvements in Type 347 have been suggested to date which would solve the problem, even though a great deal of data has been accumulated in support of many of these theories.

The second approach to the problem followed the same pattern that was used to solve the graphitization problem in the Power Industry some 10 years earlier. In 1954 the author proposed that the cracking experienced in the base metal adjacent to the weld during welding and during subsequent heat treatment or service was due to the inability of the deposited weld metal to yield or creep at the temperature of interest. This produced a metallurgical notch and a point of strain concentration at the weld metal-base metal interface. Various refinements to this theory have been proposed by other investigators, but basically the development of alternate materials or welding procedures has centered around improvements in hot ductility, and consequently the ability of the welded joint to relax at high temperature without cracking.

Fig. 1 Transverse-tensile data obtained from restrained weldments. The solution heat treatment was conducted on the integral restrained assembly

Table 1—Chemical-Analysis Range and Specified Mechanical Properties

| | |
|---|---|
| Chemical analysis: | |
| C | 0.10 max |
| Mn | 2.50 max |
| Si | 0.75 max |
| Cr | 14.50–16.50 |
| Ni | 7.50–9.50 |
| Mo | 1.00–2.00 |
| S | 0.025 max |
| P | 0.025 max |
| Mechanical properties: | |
| Tensile strength, psi, min | 75,000 |
| Yield strength, psi, min | 35,000 |
| Elongation in 2 in., %, min | 35 |

At about the same time, a development program under way at the author's company for the Bureau of Ships provided a new 16Cr–8Ni–2Mo stainless-steel arc-welding electrode. This weld deposit showed considerably greater hot ductility than conventional chromium-nickel grades. The chemical analysis range and typical room-temperature mechanical properties are given in Table 1.

In 1954 and 1955 several utilities were having troublesome maintenance problems in repairing Type 347 welds which had failed in service. In some cases repairs with Type 347 low-ferrite electrodes proved practically impossible. Other utilities planning high-temperature installations were faced with serious concern in selecting the proper material

R. D. Wylie is associated with the Boiler Division of the Babcock & Wilcox Co., Barberton, Ohio.

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for steam piping and turbines. After some initial success by Public Service Electric & Gas Company of New Jersey in making difficult repair welds with the 16-8-2 electrodes, considerable interest developed in this material in the steam power industry and many organizations contributed research time and effort in a full evaluation of these welding procedures.

The various organizations which cooperated in this work have submitted their data for summary and these are the data which form the basis of this paper. The author believes that the cooperation evidenced in this development is unique in published literature and is a credit to the various organizations involved. These organizations were: Crane Co.; General Electric Co., Large Steam Turbine Division (Rensselaer Polytechnic Institute); M. W. Kellogg Co.; Public Service Electric and Gas Company of New Jersey (University of Michigan); Westinghouse Electric Co.; Pittsburgh Piping and Equipment Co.; U. S. Navy Department, Bureau of Ships, Code 537.

In addition to these, other organizations have added their encouragement to this program in pioneering the use of these procedures: Public Service Electric and Gas Company of New Jersey; American Gas and Electric Co.; Commonwealth Edison Co.; Consolidated Edison Co.; Philadelphia Electric Co.

The data to be discussed will include tensile properties, impact-test results including the effect of aging, corrosion and oxidation resistance, high-temperature creep, relaxation, and stress-rupture data, welding tests and the results of simulated service tests.

### Tensile Tests

Extensive tensile tests have been made on 16-8-2 weld metal in the as-welded condition and after various postweld thermal treatments, including long-time aging at elevated temperatures. The results of these tests are presented in Table 2.

These data indicate that postweld solution-treatment decreases the tensile and yield strengths of deposited weld metal with a concomitant increase in elongation and reduction of area.

Aging at elevated temperatures has a similar effect on as-welded tensile properties without increasing the ductility. There was a slight increase in yield strength of solution heat-treated weld metal after aging; however, the ductility remained in the range for unaged properties.

In order to qualify a welded joint properly for ASME Code construction, a full-size joint must be welded and subjected to a series of mechanical tests. These tests include transverse tensile tests and bends. Since this welding procedure was qualified for two base

Table 2—Tensile Properties of 16-8-2 Weld Metal at Room Temperature

| *Condition* | *Ultimate strength, psi* | *Yield strength, psi, 0.2% offset* | *Elongation in 2 inch, %* | *Reduction of area, %* | *Hardness, BHN* |
|---|---|---|---|---|---|
| As-welded (range of 75 tests) | 89,000 to 98,000 | 58,000 to 69,000 | 36.7 to 47.5 | 50.1 to 67.5 | 169 to 252 |
| Solution-treated, 1925–1975° F, AC (range of 11 tests) | 84,250 to 91,050 | 34,750 to 43,000 | 51.0 to 64.0 | 56.2 to 70.7 | 150 to 169 |
| AW + 5000 hr, 1200° F | 97,500 | 50,000 | 40.0 | 66.0 | 190 |
| AW + 5000 hr, 1350° F | 98,250 | 47,000 | 42.0 | 57.7 | 162 |
| AW + 5000 hr, 1500° F | 92,250 | 41,000 | 57.0 | 57.8 | 159 |
| ST + 5000 hr, 1200° F | 91,500 | 44,000 | 47.0 | 65.1 | 180 |
| ST + 5000 hr, 1350° F | 94,000 | 46,500 | 45.0 | 59.4 | 169 |
| ST + 5000 hr, 1500° F | 91,500 | 38,500 | 55.5 | 56.2 | 162 |

Fig. 2 Square-type transverse-tension specimen—16Cr – 8Ni – 2Mo weld in 16Cr – 8Ni – 2Mo pipe

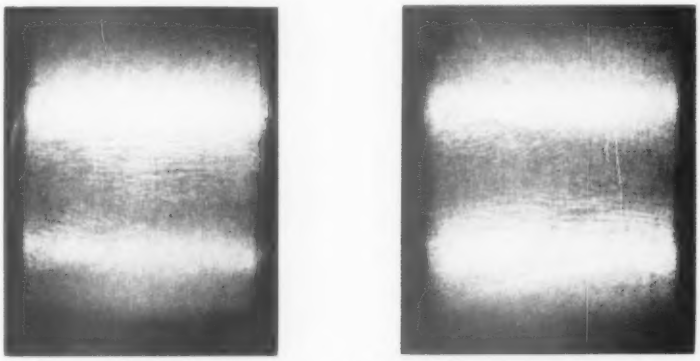

Fig. 3 Typical side-bend test samples 16Cr – 8Ni – 2Mo weld on 16Cr – 8Ni – 2Mo pipe as-welded

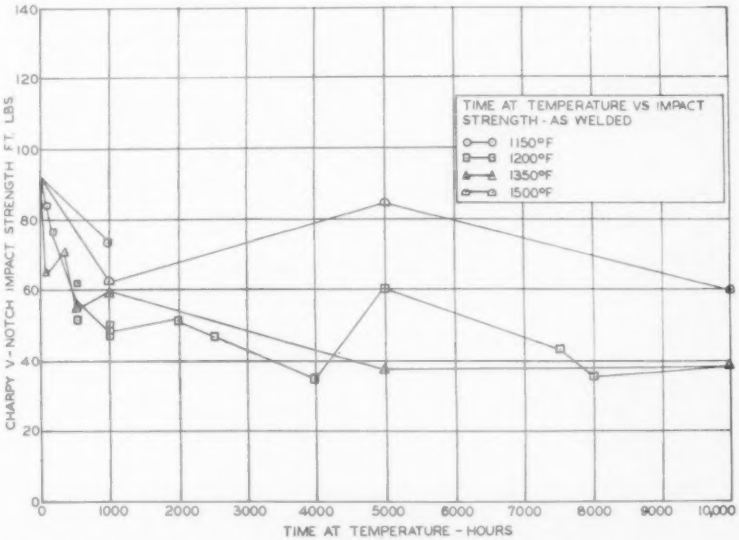


Fig. 4 Time at temperature vs. impact strength—as-welded

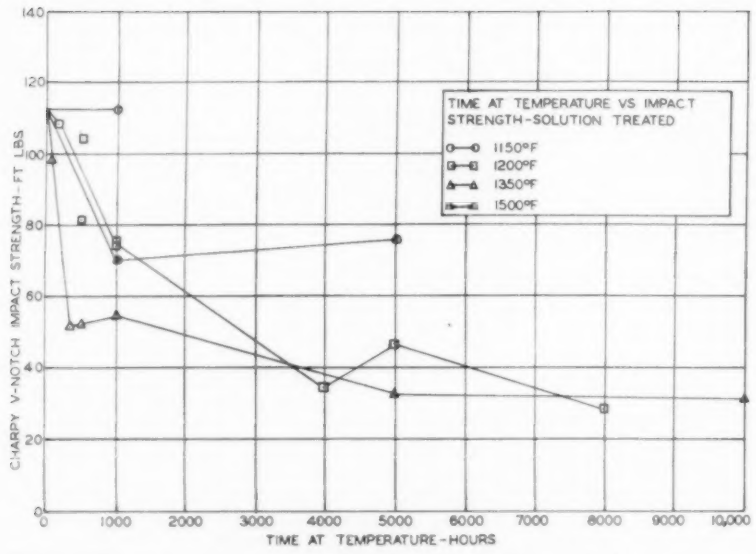


Fig. 5 Time at temperature vs. impact strength—solution-treated

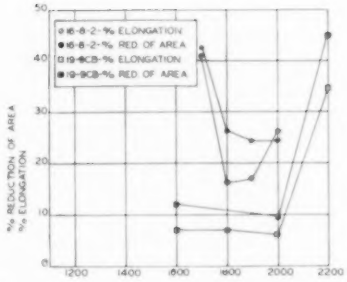


Fig. 6 Short-time tensile ductility of 16Cr – 8Ni – 2Mo and 19 – 9Cb weld metal at elevated temperatures

metals (Type 347 and Type 316) in at least three forms (tube, pipe and plate), a number of such tests was made. Typical results are shown in Fig. 1 for 0.505 tests, and Fig. 2 for square tensile specimens. All except two of the failures occurred in the base material. Typical bend tests are shown in Fig. 3.

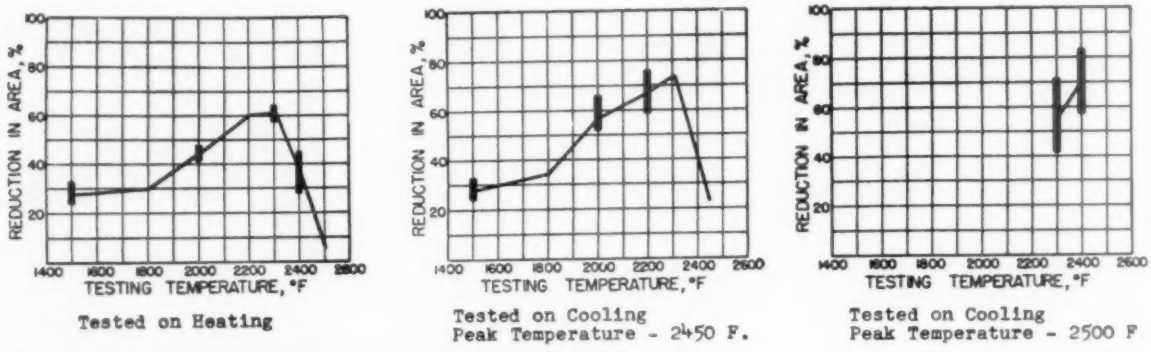


Fig. 7 RPI hot-ductility-test results—16Cr – 8Ni – 2Mo weld metal

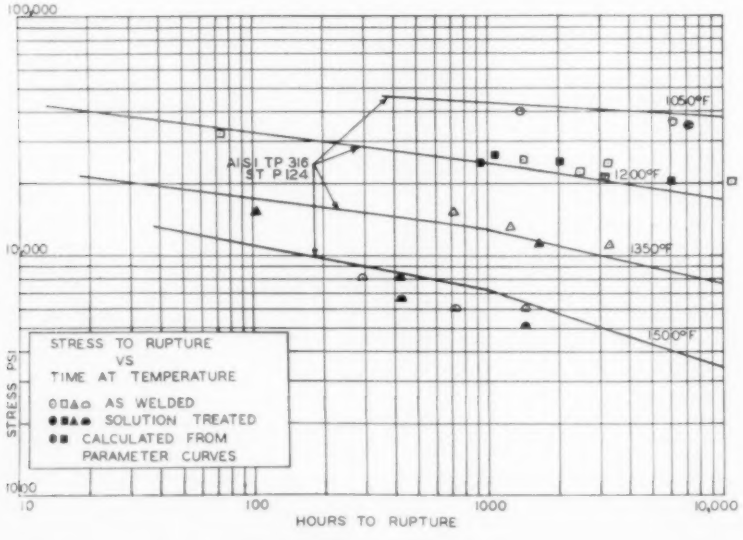


Fig. 8 Stress to rupture vs. time at temperature

## Impact Tests

One of the requirements for materials designed for high-temperature service is structural stability. Carbide precipitation and/or the formation of sigma or chi phases may have a pronounced effect on notch-toughness properties. This is particularly true in weld deposits which historically contain a small percentage of delta ferrite in the microstructure as a crack inhibitor. A summary of the impact-test data is given in Figs. 4 and 5.

An analysis of these data shows that, at the most severe aging temperatures, 1200 and 1350° F, the weld metal in both the as-welded and solution-treated conditions maintains an impact strength of more than 20 ft-lb. Indications are that, at 1500° F, the sigma phase which forms does not have so pronounced an effect on impact properties as at lower temperatures.

A comparison of the impact strength

of the 16-8-2 weld deposits with that of other commercial chromium-nickel stainless-steel deposits was presented by Hoke, Eberle and Wylie.[9] The data show that this weld deposit retains a higher level of impact strength than all other chromium-nickel stainless-steel deposits when aged without postweld heat treatment. Solution-treated low-ferrite Type 347 and Type 308 have similar notch-toughness characteristics.

### High-Temperature Properties

An extensive evaluation was made of the elevated-temperature mechanical properties of this weld deposit. Normally, tests of this nature on weld deposits are compared with the wrought materials which are to be joined. For purposes of this paper, the properties will be compared with Type 347 and Type 316 wrought material or the commercially available weld-deposited metal of these types, whichever is applicable.

The advent of the Nippes test and the work of other investigators focused attention on the use of a tensile test at high temperature for evaluating hot ductility. Such tensile tests on this material were made using 0.505-in. samples and the 0.250-in. diam specimens employed by Nippes. These data, together with a comparison of similar properties of Type 347 weld metal and wrought material, respectively, are shown in Figs. 6 and 7. As has been reported previously, these data indicate that the 16-8-2 weld deposit exhibits better hot-ductility characteristics than wrought material of the Type 347 composition. Theory suggests that this property may be responsible for the superior welding characteristics of this composition.

Stress-rupture properties were studied at temperatures from 1050 to 1500° F. A summary chart of these results is shown in Fig. 8. In this case, the solid line is a comparison curve for Type 316 wrought material from ASTM STP 124. These results indicate that at temperatures of 1050–1350° F the weld deposit is stronger than the wrought material, but at 1500° F the reverse is true. Additional rupture tests across the welded joint have confirmed these results, since failure generally occurred in the base material. At all test temperatures, the solution-treated weld deposit had lower strength than the as-welded deposits.

A few all-weld metal creep tests have been run on this weld metal, as well as some parameter relaxation tests. These data are presented in Figs. 9 and 10. These data indicate that in general the creep strengths of the as-welded deposits are considerably higher than those of the base material. The relaxation test data indicate that the 16-8-2 tends to flow or relax at lower stress levels than 19Cr–9Ni–Cb. It also indicates that the solution treatment improves the ability of the weld deposits to relax. Curran and Soldan postulate that one of the most important considerations in the successful performance of an austenitic weld is whether the residual stresses of welding can relax at a rate sufficient to avoid a stress-rupture failure adjacent to a weld.

### Corrosion Properties

A number of different corrosion tests has been run on this weld metal. These include conventional Huey and Strauss tests and air-oxidation tests. The results of these tests indicate that the 16-8-2 weld deposit reacts very much the same as Type 316 wrought material. That is, when aged at temperatures of 1050–1350° F, the material is subject to rapid intergranular corrosion under severe conditions. After prolonged times at temperature, how-

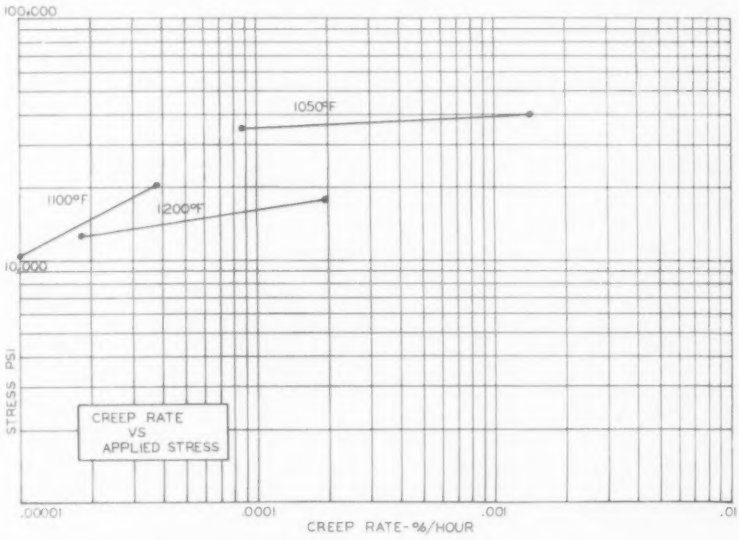


Fig 9 Creep rate vs. applied stress

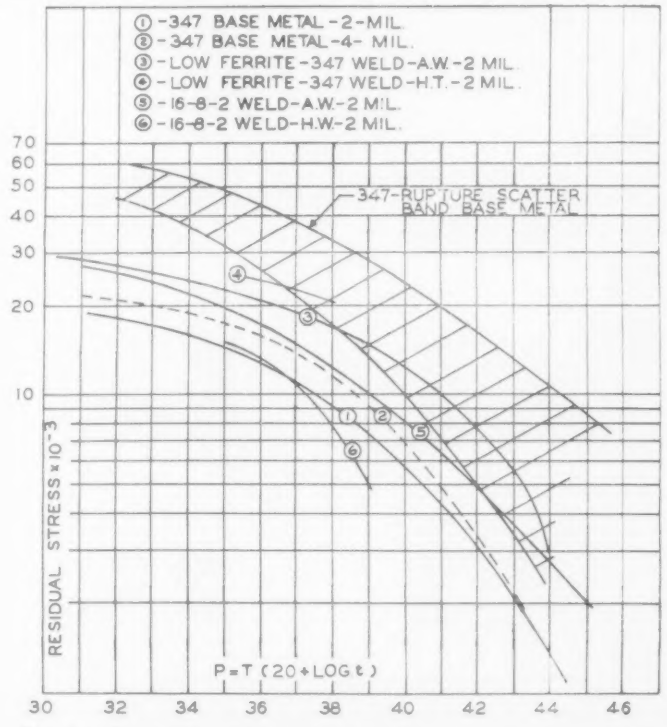


Fig. 10 Stress relaxation—parameter plot

Table 3—Huey Corrosion-Test Results Obtained from a Restrained Weldment in the As-Welded and Solution-Treated Conditions, After Aging 1000 Hr at 1200° F, 1350° F and 1500° F

| | | *Corrosion rate, inches per month* | | | |
|---|---|---|---|---|---|
| | | *16-8-2 weld metal* | | *Type 316 base material* | |
| *Aging treatment* | *Test period* | *As welded* | *Solution treated 1 hr at 1950° F, AC* | *As welded* | *Solution treated 1 hr at 1950° F, AC* |
| Initial condition | 1 | 0.0028 | 0.0019 | 0.0010 | 0.0008 |
| | 2 | 0.0035 | 0.0026 | 0.0008 | 0.0005 |
| | 3 | 0.0069 | 0.0065 | 0.0012 | 0.0016 |
| | 4 | 0.0032 | 0.0085 | 0.0005 | 0.0020 |
| | 5 | 0.0060 | 0.0101 | 0.0011 | 0.0026 |
| | Avg | 0.0045 | 0.0059 | 0.0009 | 0.0015 |
| 1000 hr at 1200° F | 1 | 0.0108 | 0.0115 | 0.0010 | 0.0039 |
| | 2 | 0.0361 | 0.0336 | 0.0038 | 0.0156 |
| | 3 | 0.0354 | 0.1886 | 0.0028 | 0.0185 |
| | 4 | 0.0294 | | 0.0049 | |
| | 5 | 0.0412 | | 0.0068 | |
| | Avg | 0.0306 | 0.0779 | 0.0038 | 0.0127 |
| 1000 hr at 1350° F | 1 | 0.0029 | 0.0047 | 0.0009 | 0.0012 |
| | 2 | 0.0049 | 0.0048 | 0.0038 | 0.0013 |
| | 3 | 0.0066 | 0.0059 | 0.0024 | 0.0018 |
| | 4 | 0.0070 | 0.0059 | 0.0025 | 0.0021 |
| | 5 | 0.0074 | 0.0065 | 0.0025 | 0.0028 |
| | Avg | 0.0058 | 0.0055 | 0.0024 | 0.0018 |
| 1000 hr at 1500° F | 1 | 0.0027 | 0.0038 | 0.0011 | 0.0014 |
| | 2 | 0.0046 | 0.0038 | 0.0017 | 0.0014 |
| | 3 | 0.0070 | 0.0073 | 0.0019 | 0.0035 |
| | 4 | 0.0091 | 0.0034 | 0.0021 | 0.0014 |
| | 5 | 0.0104 | 0.0071 | 0.0024 | 0.0027 |
| | Avg | 0.0068 | 0.0051 | 0.0018 | 0.0021 |

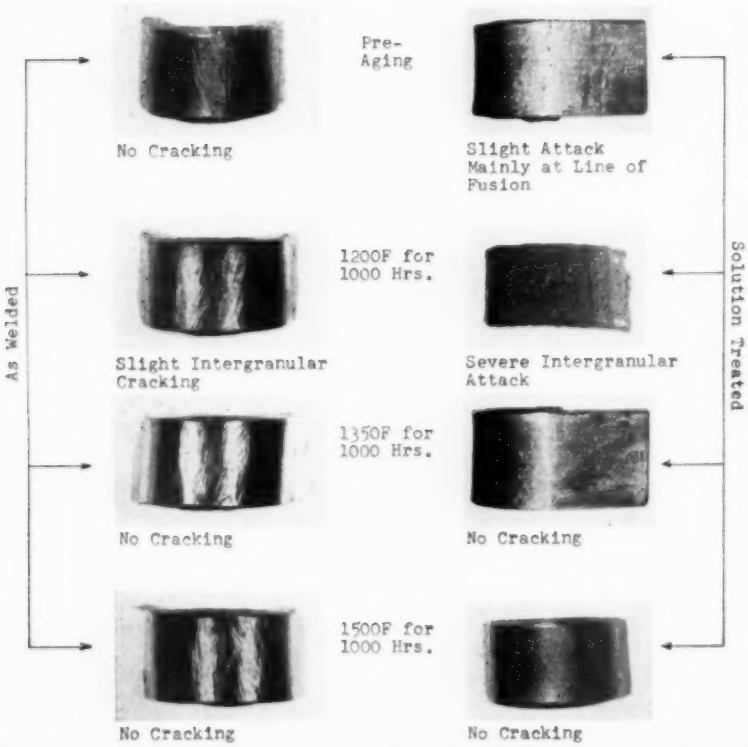


Fig. 11 Strauss test specimens after bending. These specimens were subjected to the Strauss test in the as-welded, solution-treated and aged conditions as indicated

ever, stabilization occurs through diffusion so that material which has been aged for 1000 hr at 1350° F resists intergranular attack. The typical corrosion rates of a 16-8-2 weld deposit after the various thermal treatments are shown in Table 3 and samples of Strauss test coupons are illustrated in Fig. 11. Notice, in the case of intergranular attack, that the wrought material was cracked in a similar manner. This has led to a recommendation that where this material is to be used for welding of equipment containing severe corrodants, the first pass be welded with a columbium-stabilized electrode, and that 16-8-2 be used for final passes to obtain the better mechanical properties of this composition.

The oxidation resistance of the 16-8-2 weld deposit is shown in the chart in Fig. 12. The rates indicate that at temperatures up to 1350° F this material has oxidation resistance similar to wrought Type 316; however, at temperatures above 1350° F, more rapid oxidation occurs in the weld deposit, probably due to the lower chromium content.

## Physical Properties

Physical properties are of some interest to the metallurgist. Magnetic permeability is a tool which is used to follow certain precipitation phenomena. It happens that carbide precipitation produces a change in magnetic properties of the 16-8-2 material. A plot of magnetic permeability versus aging time is shown in Fig. 13. It will be noted that, as the aging time increases, the magnetic permeability increases to a maximum, then falls off to near the original level. It is interesting that this curve parallels the effects of carbide precipitation on intergranular-corrosion resistance. This demonstrates again that redistribution of chromium probably occurs, which reduces the magnetic properties as well as improves the corrosion resistance. There is also a small amount of nonmagnetic sigma phase formed which may help to reduce the permeability.

The thermal-expansion characteristics of this weld deposit are shown in Fig. 14. This curve indicates that the expansion properties are similar to the conventional austenitic stainless-steel wrought materials.

## Microstructures

The chemical analysis of this weld deposit is balanced to provide for a minimum of ferrite in the deposit. An enlarged section of a Schaeffler diagram in Fig. 15 shows the location of a number of different commercial heats of weld metal. It can be noted that most of the weld metals fall in the range of 0–5% ferrite. Production weld deposits generally fall in the range of 0–1½% fer-

rite. As can be seen in the diagram, it is possible in this composition range to obtain a small amount of martensite in the microstructure. Evaluations of this have been made and the material appears to be able to tolerate a minor amount of this phase with relatively small decreases in ductility. After aging, the weld deposit reacts similarly to Type 316 base material in that a certain amount of sigma phase and carbides is precipitated. The quantity of sigma phase is reduced because of the lower limits of chromium and molybdenum.

### Simulated Service Tests

In order to establish confidence in this material, a number of simulated service tests has been run or is in progress. One of these tests was recently described by Emerson.[10] Some of this material was submitted to him for his tests and the results of welding Type 347 with 16-8-2 were recorded in his paper. Further tests were made on a wrought material of the 16-8-2 type. These results indicate that no failures of the type encountered with Type 347 wrought material would be encountered with this material under similar conditions.

A thermal-cycling test specimen is being prepared by Public Service Electric and Gas Company of New Jersey for installation in their Sewaren Station. This test will be thermal-cycled from 1100° F to room temperature. In this test section a piece of 16-8-2 pipe will be welded to a piece of Type 347 pipe. This test has been described in previous ASME papers.[11]

A third test installation is under way at the Philo Station of the American Gas and Electric Co. This is a small test header in parallel with the main steam line. This section will contain welds of this material.

### Service Experience

As mentioned early in the paper, a number of utilities has welds of this type in service both in the piping systems and turbines, and in superheaters at temperatures from 1050–1200° F. Experience to date has been generally satisfactory, although some cracking has developed. This is to be expected, since in general this material was used to weld joints on which extreme difficulties had arisen using Type 347 electrodes.

Experience in welding of this material indicates that not all cracking can be eliminated by electrode specification. A change in the wrought material for piping probably will be required to eliminate all of the difficulties with pipe welds in fabrication and service. However, welds made with 16-8-2 generally indicate superior welding properties.

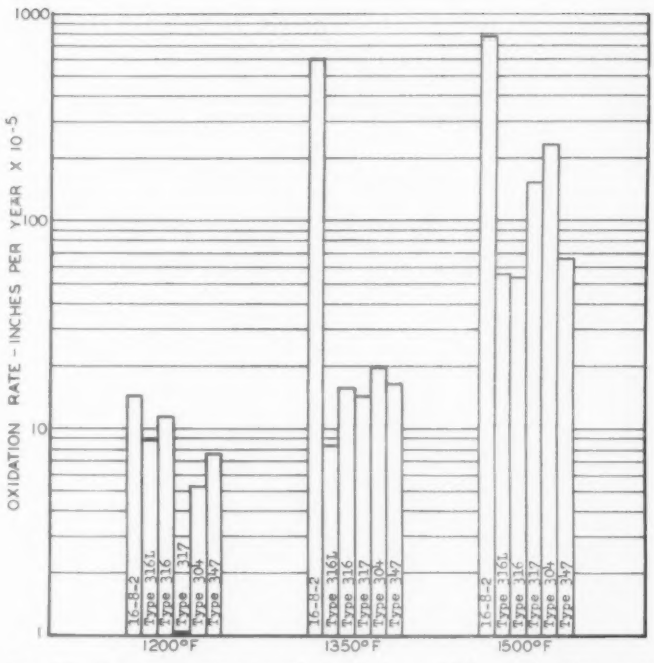


Fig. 12 Results of still-air oxidation tests on 16Cr – 8Ni – 2Mo weld deposits and base materials

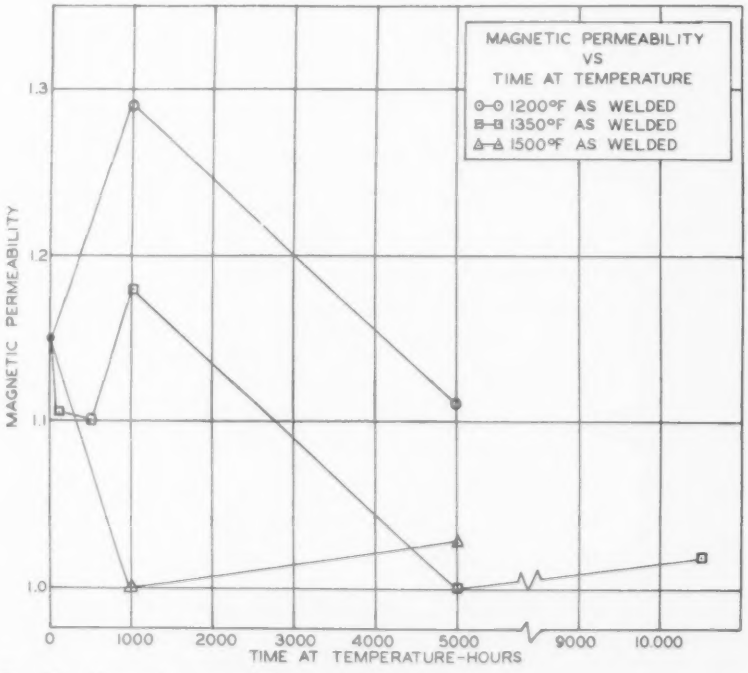


Fig. 13 Magnetic permeability vs. time at temperature

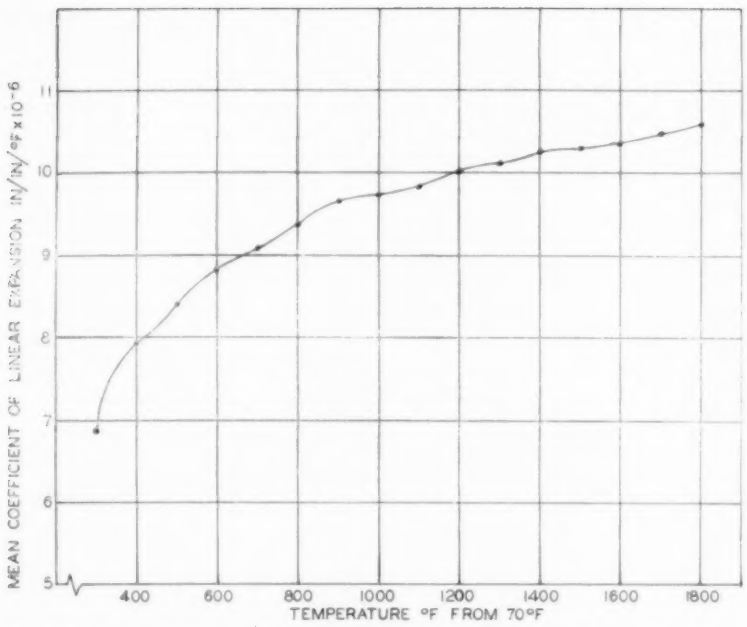


Fig. 14 Thermal-expansion characteristics of 16 Cr-8Ni-2 Mo weld deposit

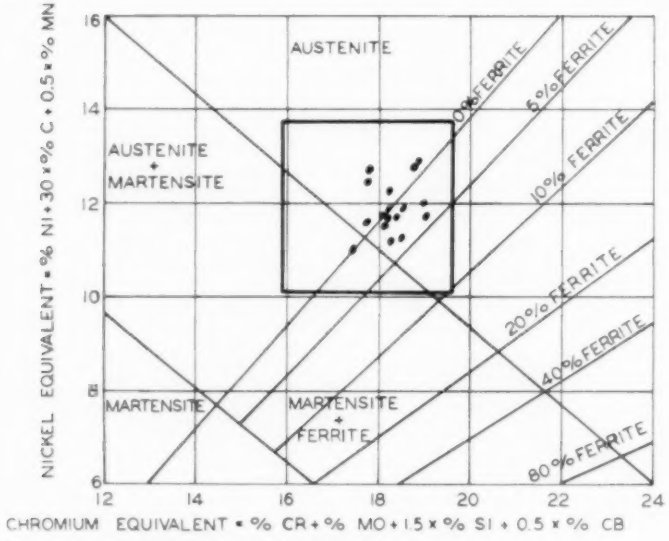


Fig. 15 Section taken from Schaeffler diagram

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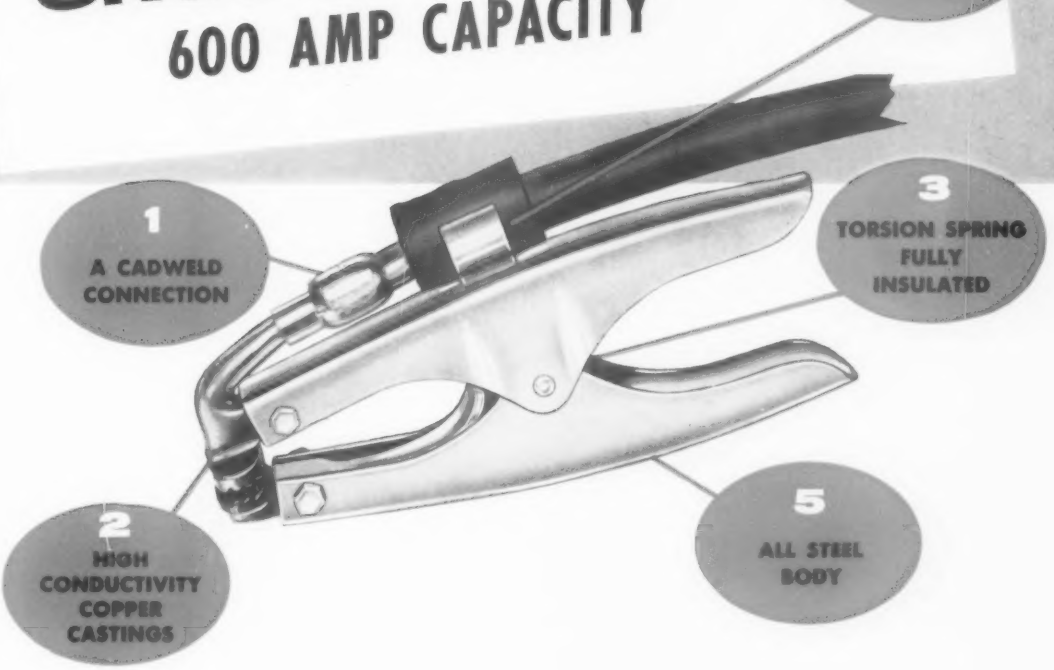
# GROUND CLAMP

## 600 AMP CAPACITY




**1** The cable is welded to the jaw by the CADWELD PROCESS. The connection has a permanent high conductivity and cannot burn up under excessive overload.

**2** The replaceable contact jaw assembly is composed of two high conductivity copper castings joined by a laminated copper conductor.

**3** The torsion spring that maintains pressure for a constant grip is completely housed by the steel body of the ground clamp. It is also fully insulated, preventing arc damage to the spring.

**4** The cable gripper ears permanently hold the cable just behind the weld. This prevents the usual flexing and fraying of the uninsulated area and greatly increases the life of the cable at the weld.

**5** All-steel body.

**CADDY® Arc Welding Accessory Div.**

***Erico Products, Inc.***

2070 E. 61st Place • Cleveland 3, Ohio

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*for machine gas cutting with natural gas at low pressure*

# 3 NEW AIRCO TORCHES!

**Also suitable for city gas, propane, and liquefied petroleum gases under pressure**

**with these outstanding features:—**

- Simplified Ease-on oxygen valve permits fast or slow opening.
- New type tip design permits tip nut to be tightened by hand.
- Mixing of fuel gas and preheat oxygen within the torch.
- Operation without booster.
- 1⅜" barrel that fits all AIRCO torch holders.
- Demountable handle that permits rack to be used in any of four positions.
- Seven straight bore type tips cut steel up to 16" thick.
- Eight divergent type tips cut steel up to 8" thick.

| Style | Overall length | Type | Type Airco equipment to be used with |
|---|---|---|---|
| 2540<br>2550 | 13"<br>14" | 2 hose<br>3 hose | Radiagraph, Camograph Monograph, Pipe Cutting and Beveling Machines |
| 2560 | 25½" | 3 hose | Travograph, Oxygraph, Duograph |




Complete information is now available on these new Airco Natural Gas Torches – data sheets, cutting charts, operating manual. Look in the yellow pages of the phone book under "Welding Equipment" for the Airco office or Airco Authorized Dealer nearest you—or write Airco direct.

AIRCO

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